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The Hows and Whys of Centrifugal Casting

By Harold B. Zuehlke*, Milwaukee, Wis.

Abstract

There are two general types of centrifugal casting: (1) That which uses a center core and (2) that without a center core where the centrifugal force of the rapidly rotating mold is used to hold the molten metal against the outer walls of the mold. The second type centrifugal casting is the one discussed in this paper. The author, by means of illustrations, shows the various parts of the casting machine, other than the driving mechanism, and then engineers an installation. He shows how the speed of the mold and the vertical taper of the outside walls are calculated and derives curves which can be used to ascertain rapidly various mold speeds. He discusses the effect of tilting the mold and how the metal is distributed in the mold. He also gives information on methods of calculating speeds and tapers of molds which are poured in a tilted position. After giving the basic engineering formulas, he discusses mold materials, especially with reference to graphite molds, mold wall protection, and mold wall draft. Other subjects include measuring the quantity of metal to be poured, the condition of the metal at the time of pouring, and special procedures for casting larger type castings centrifugally. Of special interest is his discussion of the advantages of centrifugal casting, the structures obtained in such castings, and the relative cost as compared with sand molding.

1. When a new process is being considered and developed, there are, in general, two possible approaches—one from the point of reducing the cost of an article, and the other from the standpoint of making a better article with the element of cost secondary and, in some cases, disregarded entirely. Oftentimes, both results are achieved simultaneously by a new process, and such has been

* Production Supervisor of Maritime Contracts, Allis-Chalmers Mfg. Co.

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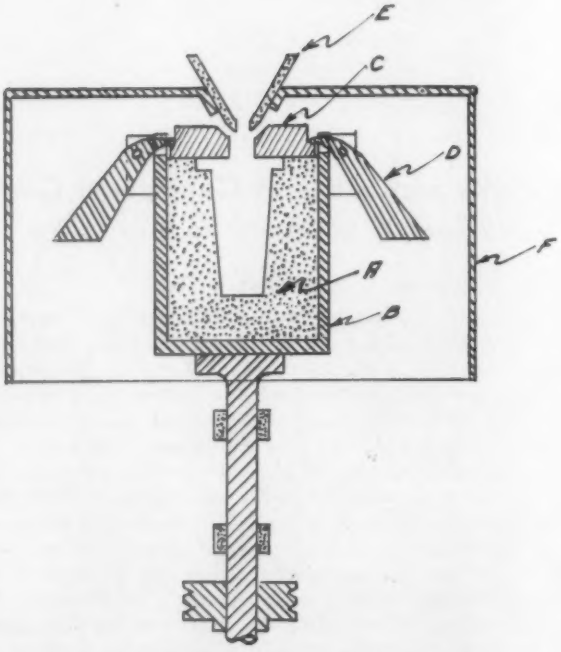

FIG. 1—CROSS-SECTION OF MOLD IN POURING POSITION, MOUNTED ON CENTRIFUGAL CASTING MACHINE. A—MOLD. B—FLASK. C—REMOVABLE COVER. D—FLY BALL WEIGHTS. E—POURING SPOUT. F—GUARD.

the experience of the Allis-Chalmers Mfg. Co. with its centrifugal casting process for non-ferrous metals, although our original approach was entirely from the standpoint of reducing costs.

Types of Centrifugal Casting

2. Centrifugal casting can be divided into two general types:
   1. Casting by using a center core.
   2. Casting without a center core and using the centrifugal force of the rapidly rotating mold to hold the molten metal against the outer walls of the mold, thus leaving a relatively straight open hole through the center of the casting.

3. The first method uses centrifugal force to feed the casting under high pressure with a gate which passes down through the center core and eliminates costly risers, thereby increasing the yield. A sand mold is used which must be stronger than the conventional mold used in static casting. This method is very

popular today with the steel foundries, and is characterized by much slower speeds than are used in the second type.

4. It is of the second type, *i.e.*, casting without the use of a center core, to which this paper is to be confined. The actual operation is quite simple, but there are many pitfalls along the way. It is the primary purpose of this paper to point out these various difficulties, indicating why they occur and what can be done to overcome them.

Description of Process

5. Figure 1 illustrates the various parts of the mold and other equipment used in centrifugal casting. Figure 2 shows the casting machine with the guard removed while Fig. 3 illustrates the casting operation. The casting machine, exclusive of the driving mechanism, consists of:

A. Rapidly revolving mold which is held central within flask *"B"*, Fig. 1.
B. Flask, shown as *"B"* in Fig. 1.
C. Removable cover, having a pouring hole which must be somewhat smaller in diameter than the hole in the piece being cast. (*"C"*, Fig. 1.)
D. Fly ball weights, which hold the cover in place when rotating. (*"D"*, Fig. 1.)
E. Pouring spout. (*"E"*, Fig. 1.)
F. Guard. (*"F"*, Fig. 1.)

6. With the setup as shown, the machine is started and a measured quantity of molten metal is poured into the mold through the pouring spout. The centrifugal force holds the molten metal against the walls of the mold, leaving a round hole through the center of the casting. When the metal has set, the machine is stopped, the cover removed, and the resulting casting withdrawn with a pair of expanding tongs. The cover can be replaced at once and the cycle repeated.

SPEED OF ROTATION

7. Speed of rotation of the mold is one of the first questions to be settled, when planning to make a centrifugal casting. To foretell the desired speed for a particular casting, it is necessary to understand centrifugal force. This force is the result of rotating a body or mass around an axis and can best be illustrated by whirling a full pail of water over one's head without spilling any of it.

Calculating Centrifugal Force

8. Centrifugal force is expressed by the formula:

(1) $$F = \frac{MV^2}{gr}$$

Where F = Centrifugal Force.

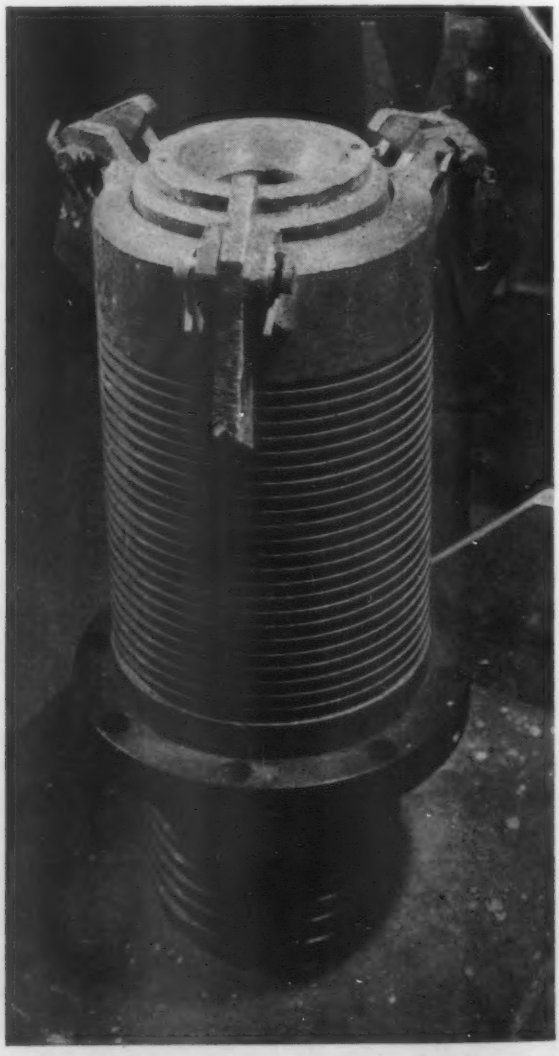

FIG. 2—CASTING MACHINE WITH GUARD REMOVED.

M = Rotating Mass.
V = Velocity in feet per second of the center of gravity.
r = Radius in feet of center of gravity.
g = Acceleration due to gravity (32.2 ft. per sec. per sec.)

Or, in terms of revolutions per minute.

$$F = \frac{M(RPM)^2 D}{K} \tag{2}$$

Where RPM = Revolutions per min.
K = Constant made up of g, π, etc.
D = Diameter in ft. at center of gravity.

9. From equation(2), it can be seen that the centrifugal force varies directly as the diameter and as the square of the speed. In other words, doubling the diameter will double the centrifugal force at the same speed, while doubling the speed will give four times the force, and three times the speed, nine times the force, etc.

Calculating Vertical Taper

10. If a casting were to be made with the shaft vertical as in Fig. 1, and then cut in half lengthwise to examine the shape of the hole which had been left, it would be found to be a tapered hole shaped like a section of a parabola. However, if we consider the side walls to be converging straight lines instead of parabolic in shape, the calculations become much simpler and the difference is negligible in the normal operating range.

11. The taper in the hole is the result of the combined centrifugal force and gravity pull, which in this case are at right angles to each other. The surface of the molten metal will finally come to rest at right angles to the resultant of these two forces, as shown in Fig. 4. The taper can be expressed as:

$$\text{Taper} = \frac{F}{M} = Tan.\ A. \tag{3}$$

Where M = Rotating Mass.
F = Centrifugal Force.
R = Resultant Force.

FIG. 3—POURING SMALL CENTRIFUGAL CASTINGS.

By substituting equation (1) in (3)

$$\text{Taper} = \frac{\frac{MV^2}{gr}}{M}$$

$$= \frac{V^2}{gr} \tag{4}$$

12. The actual mass cancels out of the equation. This means that the taper depends only on the diameter of the bore and the speed of rotation and, for any given conditions, the taper will be the same whether the casting is made from aluminum or lead.

13. After considerable experimenting with various speeds, it was found that a speed which gave a taper of 1:100 worked out the best. A taper of 1:96 would be ⅛-in. per ft. on a side or ¼-in. per ft. on the diameter of the centrifugally cast hole.

$$\text{Thus:} \frac{V^2}{gr} = 100 \text{ from (4)} \tag{5}$$

Chart Shows Speeds

14. This means that the centrifugal force on the inside of the casting must be 100 times greater than the gravity weight and can be spoken of as 100 *G*'s. A curve has been plotted, Fig. 5, for *G* values of 100, 200 and 300. To use the curve, take the diameter of the cast hole and look up its corresponding speed on the 100 *G* curve. A 2-in. diameter hole requires a speed of 1900 R.P.M. while a 4-in. diameter hole requires 1330 R.P.M.

15. It must be borne in mind that this curve is to be used only for thin-walled castings, because the *G* value increases as one goes toward the outside of the casting and if there are extremely thick walls, the *G* value can easily become dangerously high. For example, if a piece has a finish bore of 2-in., we would select a speed of 1900 R.P.M. Now suppose that this same casting has a flange which is 6-in. in diameter. From the chart, we can see that the centrifugal force at the outer diameter has increased to 300 times the gravitational force, so we would be forced to drop to lower speeds for safety sake, even if it results in more taper in the bore of the casting, unless the casting machine has been specifically designed for such excessive forces.

16. It should be pointed out that the forces on the outside of the flask will be terrific and they increase directly as the size. So,

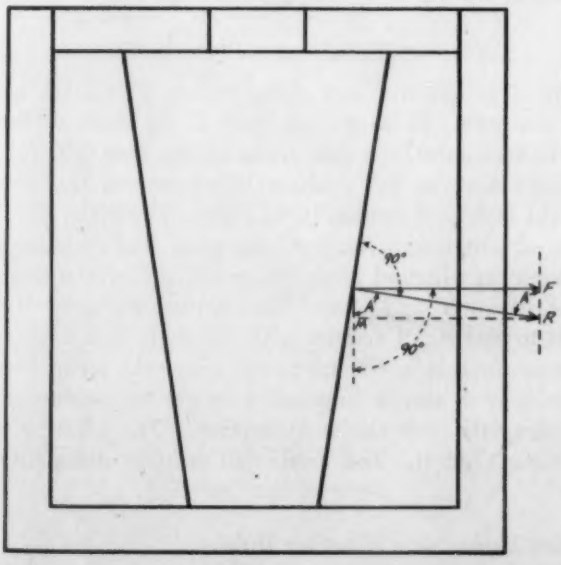

FIG. 4—VICTOR DIAGRAM OF FORCES ON FACE OF MOLD AND THE RESULTANT FORCE.

not only must the design be strong enough to withstand the speeds contemplated, but, in addition, guards should be installed to protect the workman from the danger of a possible failure. Such a guard will also give protection from flying metal, which occurs from time to time.

17. To illustrate this centrifugal force, let us take our casting with a 2-in. bore, and assume that it is just a plain bushing. From the chart (Fig. 5), its speed will be 1900 R.P.M. Now further assume that the casting machine has the cover held on by fly balls which weigh 1 lb. each and that the fly balls, when operating, are 5-in. from the center of the machine. At 1900 R.P.M., each fly ball will exert an outward force of 512 lb.

18. Some writers have stated that they use 1700 ft. per min. at the maximum diameter of any casting as a standard for determining their speed. However, by comparing the charts, you will see that this curve is considerably different from the 100 *G* curve. We do permit our operators to vary the speed of the casting machines 10 per cent from the 100 *G* curve as we have not been able to find any difference in the quality of a series of castings which have been made over a small range of speed. But we have found that, by operating as close to the 100 *G* curve as is practical, we have obtained our best results. No improvements have been noted when operating at speeds greater than 100 *G*'s.

ANGLE OF SHAFT AND POURING SPOUTS

19. Speed is not the only thing which affects the amount of taper in the bore. It is obvious that, if the shaft of the casting machine is horizontal, the side walls of the bore will form a perfect cylinder and, as the shaft is tilted toward the vertical, the taper in the bore will gradually increase. From the standpoint of the buyer of centrifugal castings, assuming that he is buying them in the rough on a pound basis, he would prefer the straight bore as the weight would be less and the machining easier. However, it is difficult to cast small articles with the shaft in a horizontal position because: first, it is difficult to introduce the metal fast enough, and, second, it is almost impossible to get the molten metal distributed correctly over the mold surface. The result is, as usual, a compromise, and the best angle can only be determined by experience.

Metal Distribution in a Rotating Mold

20. To make an intelligent guess at the best angle of tilt for any

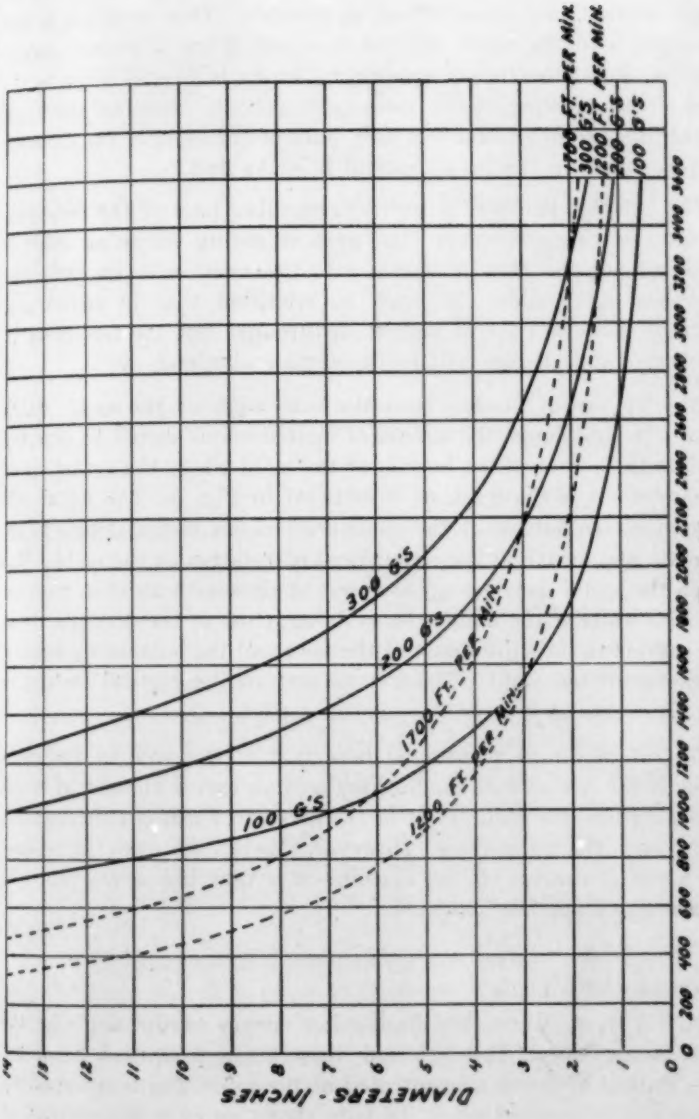

FIG. 5—CENTRIFUGAL CASTING SPEED CURVES BASED ON VARIOUS G VALUES.

job, it is necessary to understand just what happens inside of the mold. The first point to keep in mind is that the metal must be introduced into the mold as rapidly as possible, so that it will remain in one liquid mass as long as possible. This condition is best obtained with the shaft vertical machine, using a funnel shaped pouring spout the throat opening of which is just as large as the bore of the casting being made will permit. Getting the fluid metal into the mold cavity is only part of the story. The question is, just where in the cavity should it strike first?

21. At this point, it is well to remember part of the technique of sand casting, especially that part of gating where an effort is made to get the flow of metal into the mold with as little disturbance as possible. It must be admitted that in centrifugal casting, there is a lot of violent disturbance but the less there is, the better and cleaner will be the casting obtained.

22. To avoid splashes onto the side walls of the mold, which results in cold shuts, the stream of molten metal should be directed against the center of the bottom of the mold where the actual speed of movement is the least, as illustrated in Fig. 6. The metal will remain at the bottom of the mold until it has had a chance to accelerate and arrive at the same speed of rotation as the mold. But when the metal does get up to speed, it rises with an even rush up the side walls of the mold. In so doing, most of the dirt and dross are carried to the upper part of the bore and the balance up against the cover of the mold. This can be seen in the vertical casting in Fig. 7.

23. It might be mentioned here that if the hole in the mold cover is the cast size of the bushing and an excess amount of metal is poured into the mold, most of the dirt will flush out through the cover when the metal rises. However, this is very wasteful because the metal is thrown off the machine in a very fine spray and will be almost completely oxidized.

Effect of Shaft Angle

24. A very interesting happening occurs as the angle of the shaft is decreased; the dirt and dross begin to spread down the bore instead of being concentrated at the top. The best operating angle will be reached when the bore shows an even distribution of impurities over its entire length. Such a condition is illustrated in the casting at 45 degrees in Fig. 7.

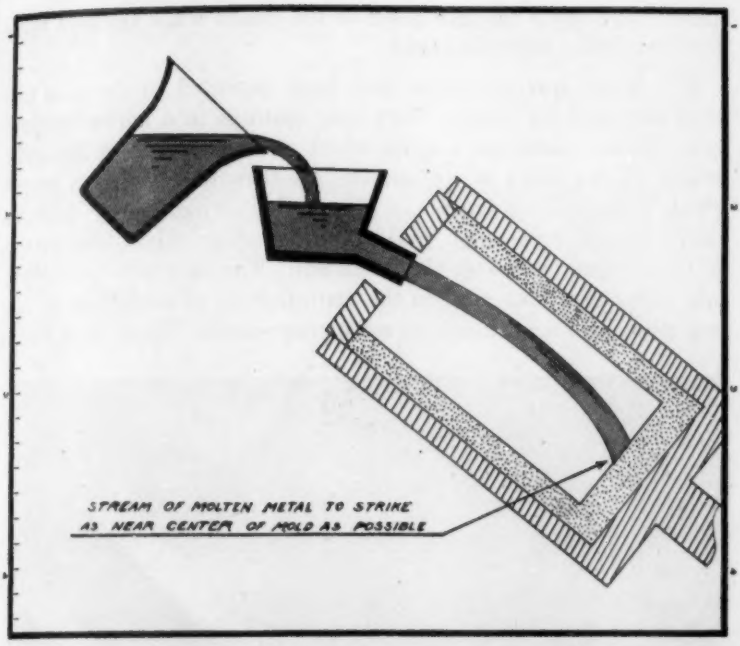

FIG. 6—POURING SPOUT DESIGNED TO POUR MOLTEN METAL TO THE CENTER OF A CENTRIFUGAL CASTING MOLD WHEN MOLD IS TILTED.

Spouts

25. If the length of the casting exceeds four times the finished bore size, it becomes a long casting. With a long casting, it becomes more and more difficult to have the metal strike the center of the mold. The spout will have to be changed at this point to a bent spout, as shown in Fig. 6. But even with this type of spout, a point will be reached where it is no longer possible to strike the bottom of the mold, and then the dirt will all be concentrated at the far end of the mold, as illustrated in the horizontal casting in Fig. 7.

26. One point about a spout is very important, and that is that the discharge end of it must be perfectly smooth with no nicks or lumps because the metal must flow from it in a smooth solid stream. A small lip or obstruction will deflect part of the metal stream and result in a poor outside finish on the casting.

27. For the funnel type spout, we use cast iron. The bent spout is a clay-lined box with a short length of steep pipe welded to it. The pipe is then lined and washed with a thin wash. The

funnel type spout fits into place in the guard while the bent spout type requires a separate stand.

28. Many special spouts have been patented in the past that were designed for making very long castings in a horizontal position. One of these was a spout which poured metal over the entire length of the mold at one time. Another was simply a trough which extended the length of the casting, which would hold the entire charge, and which could be dumped quickly. The patents on these types have long since run out. The only valid spout patents today are those used in the manufacture of centrifugally cast iron pipe, and are known as retracting spouts. These consist of a

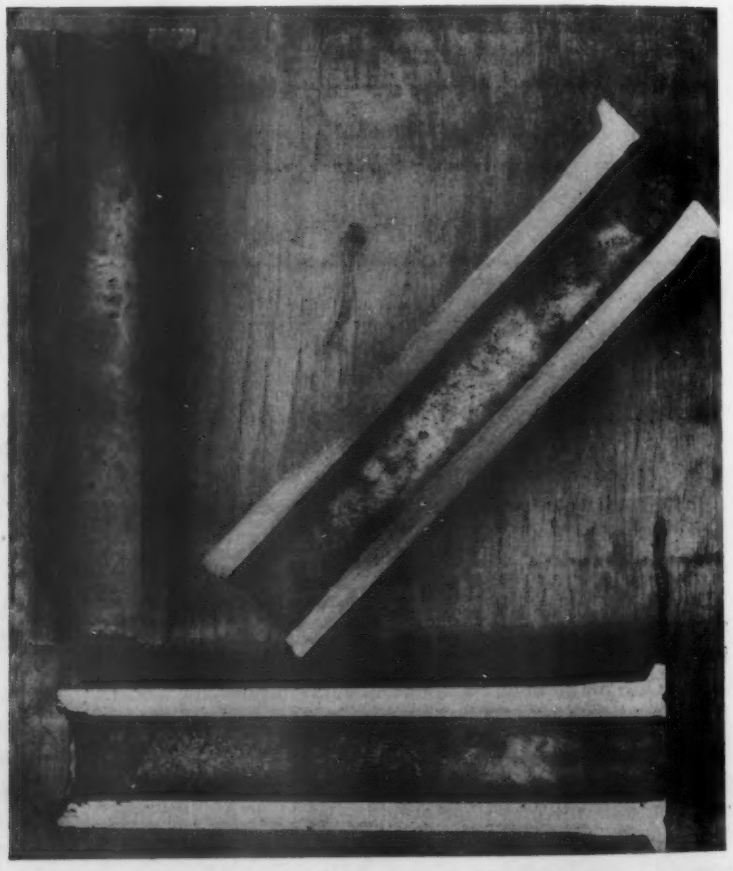

FIG. 7—CASTINGS MOUNTED IN THE POSITION IN WHICH THEY WERE POURED. NOTE TAPER AND DISTRIBUTION OF DROSS VARIATION WITH ANGLE OF POURING.

long spout which reaches to the far end of the mold and, when the pour is started, the spout is withdrawn from the mold at the proper rate so that the metal is deposited in the mold much like a coil of rope.

Calculating Speeds and Tapers with Tilted Shaft Machines

29. The following formulas may be used for calculating speeds and tapers when the shaft of the casting machine is tilted at any angle:

$$R.P.M. = \sqrt{70{,}471 \frac{Z}{R^2 - R_1^2} Sin\ a}$$

$$R_1 = \sqrt{R^2 - \frac{Z}{(R.P.M.)^2} 70{,}471\ Sin\ a}$$

Where $R.P.M.$ = Rev. per min.
Z = Length of casting
R = Radius of bore at top
R_1 = Radius of bore at bottom
a = Angle of shaft with horizontal

All dimensions are in inches.

MOLD MATERIAL

Advantages of Graphite Molds

30. Many materials have been used for molds, and to date graphite, purchased in the form of electrodes for electric furnaces, is the most satisfactory. If one takes the total cost of a finished mold, and by this is meant labor, overhead, and material costs, and divides by the total weight in pounds of good castings that are obtained from that mold, the resulting figure gives a true mold cost figured in cents per pound. The graphite is very easy to machine in a lathe and can be bored to any shape desired, so its initial total cost is very low as compared with the initial cost of a metal mold. When these costs are reduced to a pound basis, they are generally very close when the full life of the mold is utilized. For any amount less than the full life of the mold, the initial low cost of the carbon mold plays a big part.

31. For orders with a large number of identical parts (at least 200 pieces) good results have been obtained with cast iron molds.

The iron is characterized by low silicon and high carbon contents.

32. It has also been found that graphite molds are the only ones which can be used with such metals as 80-10-10 or those containing relatively high amounts of phosphorus. These alloys wet the mold surface and burn into the iron and steel molds, but will not fuse with the graphite mold. In addition, the metal lies quieter against the graphite mold. The graphite has another good quality in that it has a very low coefficient of expansion so the size of the casting made does not vary much with changes in temperature of the mold and the shape does not distort with repeated heat shocks, as do metal molds.

Disadvantages of Graphite Molds

33. There are two disadvantages to graphite molds. The first is its softness, which results in rapid wearing of the walls of the mold as each casting is withdrawn, and the hotter the mold becomes, the easier the wall wears away. As this is purely a physical destruction of the mold, the operator of the machine can extend the life of a mold considerably by taking proper care when he removes a casting. The longer a casting remains in a mold, the more it shrinks and the easier it is to remove, so too short a casting cycle does not pay.

34. The second drawback is the high heat conductivity of graphite. Both disadvantages can be partially offset by introducing powdered bentonite in the mold. This is done by blowing the bentonite into the mold axially with a spray gun, and depending upon the centrifugal force to distribute and hold it on the side walls.

Mold Wall Protection

35. The powder does two things, (1) it forms a thin insulating layer between the casting and the mold, and (2) it fills the worn and cracked sections of the mold, thereby keeping the molten metal out. It is this constant refilling of the imperfections in the mold surface with metal which causes wear. If too much powdered bentonite is used, it will get into the metal and cause lapping when the metal climbs up the walls, but a light application will remain on the mold during the pouring operation and can be brushed off the casting. It is wise to blow out the mold after each casting.

Mold Draft

36. A draft is left in all of our permanent molds to facilitate

removal of the castings. With an alloy which has a large shrinkage factor, such as aluminum bronze, we allow ⅛-in. on the diameter per ft. of length, while with the tin bronzes, the allowance is 3/16-in. on the diameter per ft. of length. This can be varied with the length and diameters of the castings as experience shows. For example, on an 8- or 9-in. diameter aluminum bronze bushing that is not over 6- or 7-in. long, there would be no draft.

Other Types of Molds

37. Another classification of molds, which can be called semi-permanent, has good possibilities. Such a mold is made with a pattern, and two or more castings can be obtained from it. Such a mold is desirable when there are not enough pieces to be made to justify a permanent mold. We have just begun on this problem, but have had good success using fire clay in the place of sand and have obtained as high as four castings from such a mold without any apparent bad effects on the mold itself.

38. The final mold type is sand, and, from the standpoint of casting, it is ideal because of its insulating properties. We have obtained very good castings using baked core sand molds, but they always result in an increase in cost. The mold itself, especially if blown, can be made very cheaply, but the difficulty of removing the casting and sand, the increase in casting time because of slower solidification, and the necessity of cleaning the casting, bring the cost of the castings up to about the same as that of the ordinary casting.

MEASURING QUANTITY OF METAL TO BE POURED

39. In a centrifugal casting without a center core, the size of the bore is determined by the amount of metal that is put into the mold, except in the case where too much is put in and then the size is determined by the hole in the cover. Two methods are available for measuring the quantity of metal desired, volume and weight. For small castings, volumetric measurements are the easiest to make, as all that is required is a series of laboratory crucibles in a range of sizes from No. 0 up. Some provision must be made for keeping them hot while in use. Hanging them over the furnace will do, but a small gas-fired oven is more satisfactory. When a heat is to be broken up into 2 or 3 castings, the best way to get the proper amount of metal to be poured is by weighing on a scale. The ideal way is to make the heat of the proper weight in

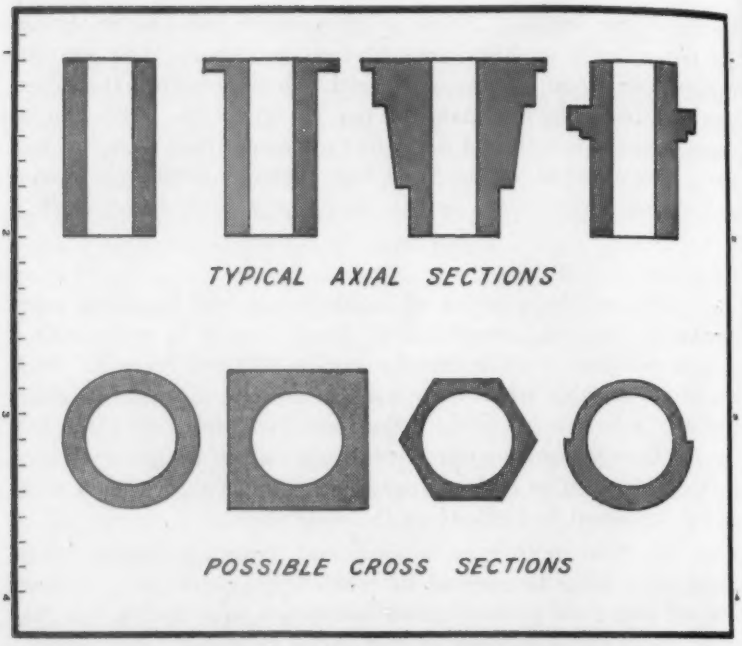

FIG. 8—CROSS-SECTIONS OF CENTRIFUGAL CASTINGS.

the first place, and then when it is ready, simply pour the entire heat into the mold. This method gives the least metal loss, but it is not practical for small castings.

CONDITION OF METAL AT TIME OF POURING

40. The procedure for pouring the metal into a measuring ladle, then into a spout, and then into the mold itself naturally raises the question, "What temperatures are required?"

41. In general, it can be said that at the instant the metal is entering the mold, the temperature can be somewhat *less* than that used in conventional casting. Since there are actually three pours made, each of which will reduce the temperature about 50°F., not to mention the loss in the measuring ladle, the furnace temperature must be 100-150°F. hotter than would be used for sand casting. For aluminum bronze, we use a melting temperature of 2100-2200°F., and with the tin bronzes, not over 2100°F. When furnace temperatures are too high, a large amount of oxide will show up in the bore, so it is easy to tell if excessive temperatures are being reached.

SPECIAL PROCEDURE FOR LARGER CASTINGS

42. As larger and longer castings are attempted, a new difficulty, in the nature of a lap or unfused layers of metal on the outside of the casting, will appear. At first thought, it would appear that, by using proportional speeds and angles, any size casting could be made. However, such is not the case, because the molten metal cannot be introduced into the mold cavity in the same length of time as with a smaller casting. The important point is to get the entire mold surface covered by a layer of molten metal, and then to follow with more metal before the first layer has had a chance to freeze.

43. For example, a 3-in. diameter casting, 6-in. long, has 56.6 sq. in. of mold surface to be covered, while one 6-in. in diameter, 12-in. long, has 226.5 sq. in., or four times the surface. This means that the rate of pour must be increased four times to pour the larger casting at proportional speeds, *i.e.*, 100 *G*'s. As good practice requires that the smaller casting be poured as rapidly as possible, it follows that it would be impossible to pour the larger one four times faster.

44. The solution to this is to start the pour with the casting machine at rest, and then start the machine when one-fourth to one-third of the pour has been completed. This provides sufficient molten metal to completely cover the mold surface in the first rush up the walls. Generally speaking, the angle will be somewhat less, or flatter, when using this procedure. Another reason for using a flatter angle with a larger casting is to facilitate its removal from the mold.

45. The greater the acceleration of the machine the better, hence the motor should be larger than would be required for normal starting. The actual power required to maintain rotation is very small.

ADVANTAGES OF CENTRIFUGAL CASTING

1. Clean Metal

46. As the oxides, dross, etc. which are normally found on the surface of molten metal are lighter than the metal bath, they will be forced through the molten metal towards the inner surface of a centrifugal casting while it is rotating. If the metal remains molten long enough (actually only a few seconds), these impurities will work completely through the metal to the inner surface where

they can be removed by machining. If dirt or foreign inclusions are found in the metal itself, it is a sign that the metal is chilling too quickly. This can be offset by having the metal hotter at the instant it enters the mold, or by more preheating of the mold. Increasing the speed of the casting machine also will help, as the molten metal assumes its final position more rapidly.

47. Care must be taken when stepping up the speed of the machine to see that:

(1) The machine will withstand the additional stresses of the higher speed.

(2) The increase in speed will not result in segregation of the constituents.

FIG. 9—POURING AN 80 LB. CENTRIFUGAL CASTING.

48. In the case of a true alloy, such as aluminum bronze, there is no segregation even at much higher speeds than are recommended. But with leaded bronzes, segregation becomes appreciable when the lead content goes above 12 per cent and a reduction in speed is necessary when working with these bronzes.

49. *Rate at Which Impurities are Forced Through Molten Metal.* The rate at which the impurities are forced through to the center of a centrifugal casting increases directly as the G factor. Consider a molten metal which has a unit weight of one pound having impurities with a unit weight of three-fourths pound. Under static conditions, the impurity will be pushed to the surface by a force which is the difference between the unit weights or one-fourth pound. When this mixture is rotated at a rate which gives 100 G's, the centrifugal force on a unit of the metal is 100 lb. and on the impurity of 75 lb. The force which pushes the impurity to the center is again the difference between the two and, in this case, 25 lb.; so the tendency to separate has been increased 100 times. Hence, a cleaner casting is both theoretically and actually possible by the centrifugal process.

50. Entrapped gases are purged in the same manner and absorbed gases, which are not given off until freezing occurs, still have an avenue of escape through the adjacent molten metal to the center. Under some conditions, air will be trapped between the outside surface of the casting and the mold, and the surface tension of the molten metal prevents its escape. However, the pressure of the molten metal is so great that these air pockets are compressed to a fraction of their normal size and seldom exceed 1/32-in. depth.

2. Pressure Feeding and Progressive Solidification

51. While the metal is molten, it is under considerable pressure against the mold faces. As the freezing will start at the mold surfaces and proceed towards the center, there will always be molten metal under pressure to feed the casting. There is a limit to progressive solidification, and it is reached when the wall thickness of a centrifugal casting becomes so great that the rate of heat transfer from the molten section of the casting through the exposed ends of the mold is about equal to that back through the previously frozen material. If the casting is thicker than this limit, freezing will then start at the inside of the casting, and this will result in a spongy and porous layer just as though a static casting were poured without a riser.

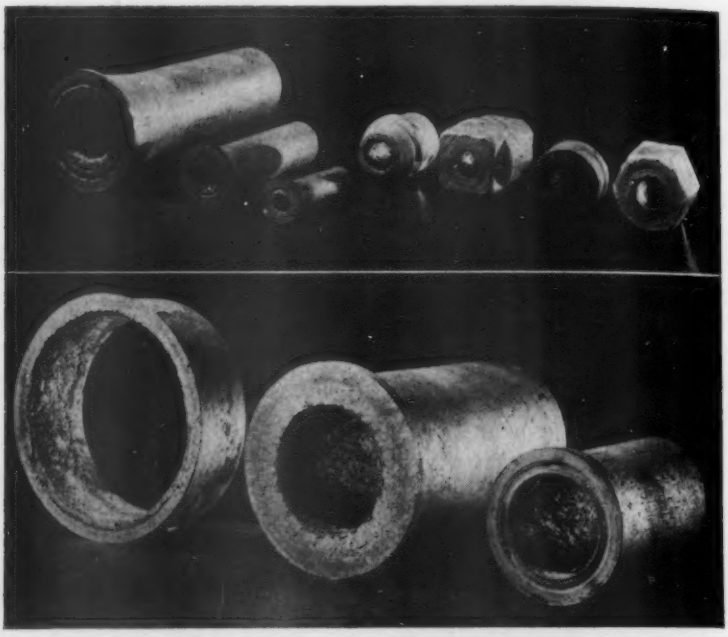

FIG. 10—TYPICAL CENTRIFUGAL CASTINGS.

52. Up to the limit mentioned, the density of a centrifugal casting will be slightly greater than that of a similar static casting because of the smaller voids between crystals. The actual increase in density will be about 0.5 per cent.

53. The spongy condition in thick-walled castings can be somewhat improved by making the last half of the pour much slower than at the start, in order to maintain as high a temperature as possible at the center of the casting. This corresponds to adding hot feed metal to a riser in a static casting. Another aid is to throw some powdered charcoal into the mold as soon as the pour has been completed.

GRAIN STRUCTURE

54. A centrifugal casting shows a slightly finer grain than a sand casting and the grain structure is generally radial, following the direction of solidification. With non-ferrous castings, the tensile strength is slightly higher in centrifugal castings than in those static cast, but, under certain conditions, an iron casting will

show slightly lower strength. This is probably due to the grain structure being too regular instead of interlocking.

COSTS

55. When permanent molds are used, castings can be produced cheaper by the centrifugal process than by the conventional manner. In the first place, there is a great increase in yield as there are no gates, sprues, or risers on the castings and yields up to 95 per cent are possible. This not only saves the remelting costs, but also keeps the foundry free from an accumulation of special mixes. The cleaning room costs are almost completely eliminated because all that is necessary is to grind off the thin flash that sometimes occurs between the cover and the mold.

56. There is an additional saving in the machine shop which does not show in the foundry costs. When the bore in the mold is concentric with the axis of rotation, the resulting hole in the center of the casting will be true with the outside, so no extra allowance need be left for possible eccentricity of the core. This reduces the cost of machining the part. The outside finish also is more regular than on a sand casting, so that less stock should be allowed on a centrifugal casting made in a permanent mold. A machining allowance of 1/16-3/32-in. on a side is usually enough, but experience is the best guide in this respect. On the bore, slightly more stock must be allowed (⅛-3/16-in.) because of the accumulation of dross and other impurities.

57. Another machine shop saving is through the use of the outside surface of a centrifugal casting as cast instead of machining it. This not only saves in the material used in the casting, but also in machine time.

58. Because of the consistent soundness of centrifugal castings, it has been possible to substitute them for rolled sections which require a lot of machining and removal of excess stock to get them into their final form. We do not mean to say that there is no scrap, but the scrap that is found shows up on the first turning and boring operations. When the process is followed through correctly, there will be no hidden defects, such as gas pockets, porosity, etc., which might show up at some later time.

CONCLUSION

59. There are three fundamentals to successful centrifugal casting:

(1) A uniform and quick distribution of molten metal over the entire surface of the mold.

(2) Progressive solidification of the metal from the mold surface towards the center.

(3) Final accumulation of all the dirt and impurities in the bore where they can be removed by machining.

60. If a centrifugal casting is not good, one or a combination of these three things is not taking place.

DISCUSSION

Presiding: G. K. DREHER, Ampco Metal, Inc., Milwaukee, Wis.

R. H. GILBERT[1]: How do the physical properties of the castings compare when using a metal permanent mold and one of sand?

MR. ZUEHLKE: We get about the same effect that you would with the conventional static casting, namely, the physical strength is increased at a sacrifice to some elongation.

A. V. DEMARCO[2]: We have experimented with vertical versus horizontal castings. Our findings show that we are limited on vertical castings to a length of 20 to 30 per cent greater than the outside diameter. Lengths greater than this figure produce an excessive inside taper.

The formula given in this paper for calculating the inside taper is derived from the parabolic formula, simplified to a straight taper over the section considered. Our results show that the actual parabola exists when the length of the casting reaches a figure of approximately twice the outside diameter. In this case a solid bottom exists which does not open up until excessive speeds are employed, which results in a sacrifice in structure.

The casting shown in Fig. 7 is interpreted to represent a cross section of a vertical casting. I would like some information as to what speeds were used in opening up the parabola which our practice would produce if the casting were made at 100 G's.

MR. ZUEHLKE: The actual speed used to make these castings was 1900 R.P.M. Unfortunately, when we made that illustration the photographer neglected to insert a scale. The center of the casting is about 1½-in. and the length is about 10-in.

When operating with speeds which give what we call 100 G's, we have never had a casting which has been cast vertically that showed a solid bottom, except in the case of a very cold mold. Then, it was simply a very thin layer that had been chilled at the moment of the introduction of the metal.

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[1] General Bronze Corp., New York, N. Y.
[2] Hampden Brass Co., Springfield, Mass.

MR. DEMARCO: The actual formula used in calculating the inside taper of a vertical centrifugal casting is a parabolic curve. So closely does the actual cross section follow this theoretical curve that, by standardizing other conditions, it is possible to calculate the actual weight of the casting to within ¼-lb. in some cases. We have found that the shape factor beyond the ratio of 2 to 1 of height to outside diameter in a vertical casting is no longer a straight line function, but rather the actual parabola, resulting in a closed bottom.

MR. ZUEHLKE: What actual speeds were you operating under?

MR. DEMARCO: For experimental purposes, we have operated at speeds up to 2000 R.P.M. on bushings of 2-in. outside diameter and 1¼-in. inside diameter. With lengths greater than 5-in. we still retain the closed bottom plus a "bellying" effect near the center point. Pouring temperature has only a minor effect on the shape when chilled molds are employed.

MR. ZUEHLKE: That is one of the interesting things about centrifugal castings. I cannot agree with the gentleman because we have never found the complete parabola occurring in the actual castings at 100 G's. I will admit, however, his first contention, that the inside surface is a parabola because the formula, as presented, is a parabolic equation and, if plotted, it will give a parabola. Nevertheless, operating at the speeds recommended, you are so far from the origin of your parabola that you do not (at least we have never done so) encounter this solid bottom. Just how thick was that bottom?

MR. DEMARCO: The thickness of the bottom depends on the speed. If molten metal is poured in a still mold, a flat disc will be the result. As the mold is started, and velocity increased, the top of the disc will assume the form of a parabola as the resultant of the opposing centrifugal and gravitational forces. If enough metal is poured into a mold to form a disc 3-in. thick, then the center point of the disc is the origin of the parabola and the "bottom" is 3-in. thick. Increasing the velocity drives this origin toward the bottom of the mold, until some speed is reached where an increase seems to have little or no effect in overcoming the gravitational forces. In a casting of length twice the outside diameter we find this occurring at approximately 2500 linear ft./min. at the outside diameter. However, at that speed we found that the quality of the casting suffered.

MEMBER: Did you introduce the metal in the same way?

MR. DEMARCO: Yes. We agree with the author on the mode of metal entrance.

N. JANCO[3]: Regarding vertical true centrifugal castings, which are of the type that we are discussing here, I use the equation which is exactly the same as the one given in the paper, and this follows down the line of thought of the last speaker. It is precisely the same. Your formula has the sign alpha in here, which merely takes care of the fact that you might operate the machine at an angle.

The point that I would like to bring up is the author's statement that,

[3] Centrifugal Casting Machine Co., Tulsa, Okla.

when the castings are spun at a speed sufficient to develop 100 G's, the taper will be approximately ⅛-in. per foot. That is correct. If you calculate what the taper will be, it will be ⅛-in. per foot, very closely. As mentioned in the paper, if you used 96 G's, it would be exactly ⅛-in. per foot, which also is absolutely correct. Now, does the author mean that he spins short castings about a vertical axis?

A long casting is defined as one whose length is four times the inside diameter. When you get into a long casting, do you pour vertically, horizontally, or at an angle? The paper did not mention whether you change the angle or position when pouring a long casting. When pouring this type of casting, do you not find that the taper is excessive? It is quite amazing that you made these long castings with exactly ⅛-in. taper per foot for, if you calculate the taper, that is what it will be. I have found, in castings in the short range of length, that the taper obtained does not exactly check with this formula. Actually, a greater taper is obtained than is calculated. The amount of deviation from the formula seems to be influenced by the metal poured, the pouring temperature of a particular metal as well as the material; that is, whether you use a metal mold or sand mold, and the roughness of the surface of the mold itself.

Experimental work is being performed to determine factors for different metals. These factors could then be inserted into the theoretical formula to be able to predict more accurately the taper to be expected. Has any experimental data concerning the actual taper been obtained?

Almost invariably we found—I speak about myself and the many different foundries doing centrifugal casting work—that the actual taper is greater than the calculated taper and seems to vary with different metals. Of course, pouring temperature has some influence upon it, which probably boils down to the viscosity of the metal being poured. This would be influenced by the metal alloy and the pouring temperature, since we find that it does not check the formula, and since we would like to be able to make a casting of the proper desired shape. We should like to know the shape before we pour metal into the mold, and we are doing experimental work to determine which factor should be inserted for different metals, under various conditions, so that we can predict the actual taper that we are going to obtain.

There is one other point which I would like to mention, pertaining to some of these larger castings. There are men in this room who are making larger castings centrifugally with consistent success. So, the answer is not the rate of pouring, but that there are other conditions involved as well.

MR. ZUEHLKE: With regard to Mr. Janco's first question, when we get into the long castings, we let the mold incline towards the horizontal.

MR. JANCO: Have you found that the calculated taper checks the actual taper obtained?

MR. ZUEHLKE: Only roughly, and your observation that the taper is greater than the formula indicates has been borne out by our experience.

MR. JANCO: We are doing work on that, both non-ferrous and steels. That is, castings are made in steel and iron by these same methods.

MR. ZUEHLKE: We have discussed the theoretical possibility of predetermining the shape of our casting, but we never had an application where it was desirable.

MR. JANCO: One other point: You mentioned that the outside diameter of all the castings that you make are tapered, I believe, ⅛-in. to the foot. Do you find that to be necessary, or is it simply desirable?

MR. ZUEHLKE: We found it desirable from the standpoint of the length of the life of the mold.

MR. JANCO: Even with the use of metal molds?

MR. ZUEHLKE: Yes. It makes for a faster casting cycle because the casting can be withdrawn more quickly. In the case of graphite molds, it adds considerably to the life of the mold. However, with alloys that have a high shrinkage, such as aluminum bronze, when we get to large diameters where the length is not greater than its diameter, we frequently do not use any taper, simply a straight bore.

H. J. ROAST[4]: I think a word might be given to those who, at the moment, are not considering active work in centrifugal castings. My advice to our own company was that we would have to go into centrifugal casting if we intended to survive in our other normal railway work after the war, and I am quite convinced of the accuracy of that prophecy. Therefore, it might be well for those of you who have not gone into the centrifugal field to look into it. We have only just begun and, no doubt, troubles lie ahead of us, but we are aware of the possibilities that the work will offer.

[4] Canadian Bronze Co., Ltd., Montreal, Canada.

Report of Steel Division Committee on Radiography

1. Your Radiography Committee desires to review the important developments that have taken place in the field of the industrial radiography of steel castings during the year (April 1942 to April 1943).

2. During the year a number of steel foundries that have never used radiography before are now constantly using it in development or inspection of castings. Also, the Government has purchased additional radium capsules and x-ray apparatus for casting inspection purposes. Several of the steel foundries have installed million volt x-ray equipment.

3. The Bureau of Ships, Navy Department, released during August 1942 a new set of Radiographic Standards for Steel Castings. These Standards supersede the Gamma-Ray Radiographic Standards of the Bureau of Engineering for Steel Castings for Steam Pressure Service, dated September 1, 1938.

4. The radiographs contained in the Standard are intended to be used for comparison with radiographs from actual castings when either x-ray or gamma ray inspection is required. Each plate showing defects contains two radiographs of the same defect, one being made using x-rays of 220 kv maximum and the other using gamma rays.

5. The plates are divided into groups according to the type of defect, as follows:

Group A—Gas and Blowholes
Group B—Sand Spots and Inclusions
Group C—Internal Shrinkage
Group D—Hot Tears
Group E—Cracks
Group F—Unfused Chaplets
Group G—Unfused Chills

Casting Classification

6. Castings have been grouped into five classifications, in accordance with their service requirements, size wall thickness, etc. It is intended that the casting classification will be indicated on the marked plans in conjunction with each symbol representing a

Note: This report was presented at a Steel Castings Session of the 47th Annual Meeting and Second War Production Foundry Congress, St. Louis, Mo., April 29, 1943.

particular film location. In cases where the casting classification is not shown on the plan, this classification may be determined by reference to Table 1.

Table 1

CLASSIFICATION OF CASTINGS FOR APPLICATION OF RADIOGRAPHIC STANDARDS

Class	*Casting*
1	High pressure valves and fittings (wall thickness less than 1 in.).
	Super-heater fittings.
	Boiler stop valves.
	Machinery castings subject to fatigue or impact stresses (wall thickness less than ½ in.).
2	High pressure turbine casings.
	Steam chests.
	Turbine throttle valves.
	High pressure valves and fittings (wall thickness 1 in. or greater).
	Low pressure valves (wall thickness less than 1 in.).
	Machinery castings (wall thickness ½ in. or greater).
3	Low pressure turbine casings.
	High pressure turbine exhaust casings.
	Low pressure end of high pressure turbine.
	Low pressure valves (wall thickness 1 in. or greater).
	Machinery castings not subjected to steam pressure.
4	Hull castings less than 3 in. in thickness—subjected to high service stresses.
	Machinery castings not subjected to impact stresses or vibration.
5	Hull castings 3 in. or more in thickness and subject to high service stresses.

7. Various degrees of acceptability are permitted for each class. Acceptability is rated as before in three grades: acceptable, borderline and unacceptable.

8. Rejected castings may be repaired by welding in accordance with Appendix VII, Section C-3 of Navy Department General Specifications for Inspection of Material. The acceptability of repair welds will also be determined by radiography and by comparison with radiographic standards for Class A-1 welds.

9. Radiographic Standards may be viewed at the office of the local Naval Inspector.

10. Committee E-7 of the American Society for Testing Materials presented a symposium on Radiography during the past year. None of the papers dealt directly with steel castings, but information applicable to the radiography of steel was presented. A list

of these papers is given in the bibliographic section of this report.

11. During the year an official publication of the Industrial Radium and X-ray Society known as Industrial Radiography appeared, and three issues of this magazine were issued during the year.

12. In the past your Committee has attempted to review all the publications on radiography of steel and fundamentals relating to radiography. However, the number of articles being prepared on this subject have increased considerably with the increased use of radiography. It is believed, therefore, that the Committee should limit itself to reporting on matters, the subjects of which are related to the radiography of steel castings. Other items, such as articles on technique and radiographic principles, can best be handled by the new Industrial Radium and X-ray Society, and reviewed by them. This policy is in line with the one formulated some time ago when it was decided that your Committee would not attempt to formulate radiographic specifications, but that such activity was the province of the American Society for Testing Materials, and that your committee would merely report on A.S.T.M. doings and stand ready to be of assistance to them on matters pertaining to steel castings. Technical articles on the radiography of steel castings have appeared in various technical publications during the year. These articles are listed in the bibliographic section of this report as references for the radiographer in the steel casting industry.

Respectfully submitted,

COMMITTEE ON RADIOGRAPHY

C. W. BRIGGS, *Chairman*
J. J. CURRAN
J. A. DUMA
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W. C. HAMILTON
JOHN W. JUPPENLATZ
E. W. PAGE
A. P. SPOONER
H. F. TAYLOR

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A Report on the Use of Aluminum in 10,000 Acid Electric Heats of Steel

By E. C. Troy*, Philadelphia, Pa.

Abstract

A report covering the application of 0.10 per cent to 0.15 per cent aluminum addition to acid electric steel for castings in green sand. Melting practice and methods of adding aluminum are explained. Effects of aluminum additions on the casting properties of the steel are covered, and physical properties experienced are tabulated.

1. To date, no material, other than aluminum, has been found which will fully protect against pin hole porosity when pouring steel in small green sand molds. Because aluminum did protect against pin holes, it became a necessity to the manufacturer of smaller castings made in green sand. Attendant with its use for protection against porosity of this nature was the undesirable drop in ductility of the metal when tested cold by tension or bend. The problem rapidly became one of discovering ways to obtain the advantages of its use without the disadvantages. The foundry metallurgist, unknowingly, made the matter worse by keeping the aluminum additions to the steel at a minimum. Attempting to produce a hairline balance between the aluminum addition, degree of metal oxidization and mold conditions resulted in frequent pin holes, lowered ductility, or both.

2. The foundrymen are deeply indebted to C. E. Sims and F. B. Dahle of the Battelle Memorial Institute for their paper, "*Effect of Aluminum on the Properties of Medium Carbon Cast Steel.*"[1] The Dodge Steel Company immediately attempted to put the evidence gathered therein to practical usage. The results were encouraging and checked with the authors' data when applied to

* Metallurgist, Dodge Steel Co.

1 Sims, C. E., and Dahle, F. B., "*Effect of Aluminum on the Properties of Medium Carbon Cast Steel*," Transactions, American Foundrymen's Association, vol. 46 (1938), pp. 65-132.

Note: This paper was presented at a Session on Steel Castings at the 47th Annual Meeting, American Foundrymen's Association, St. Louis, Mo., April 30, 1943.

the melting, molding and foundry practice used and the type of castings produced at our plant.

3. This present report covers more than four years' experience while using 0.10 per cent to 0.15 per cent aluminum additions to acid electric steel for green sand castings. Of the approximately one and a half million castings made during this period, about 90 per cent were carbon steel, the balance being various intermediate alloy grades, including 5.0 per cent and 13 per cent chromium.

4. Castings made ranged in weight from a few ounces to 4000 lb. Most of them were less than 50 lb., with the average weight at about 22 lb., as our plant is a jobbing shop making a large number of pressure castings for steam and oil line application. All molds were green sand without surface preparation such as spraying or drying. The molding facing sand used was made up of approximately 3.5 per cent moisture, 5 per cent bentonite, one per cent cereal binder and the balance washed and dried subangular silica sand of about 78 A.F.A. fineness, the final green permeability being about 90 for both new sand facing and old sand backing. Molds were poured from a few minutes to several hours after closing, depending on the capacity of the melting unit to keep up.

5. The practice of higher aluminum additions to the steel depends for its success on properly made steel, deoxidized in such a manner that aluminum, when added, will, in part, remain unoxidized. The amount of aluminum addition necessary will vary with the condition of the steel and slag at the time of addition. Generally, the addition necessary will be between 0.10 per cent and 0.15 per cent, and future references to high aluminum treatment will be on the basis of this range.

6. The change in sulfide formation with varied aluminum addition accounts for the variation in ductility and is fully described by Sims and Dahle. The writer does not feel that he can add anything other than confirmation to the report of these two investigators. He does, however, intend to cover some of the cares to be exercised and the troubles likely to be encountered with the use of high aluminum additions.

MELTING PRACTICE

7. In 1935, the writer's company set up a simplified melting procedure which, with but small modification, is practiced to this day. The procedure embraces the thought that simplification makes for easier reproducibility. It is evident that if the charged scrap can be brought by vigorous boiling to a condition of near

pure iron and then alloyed with carbon, manganese, silicon and other required elements, the final product will be the steel desired. Experience has taught that satisfactory metal generally results if the boiling action of the bath, having once been induced, is continued up to tapping.

8. In order to simplify the entire process, the materials necessary for the above procedure are chosen with regard to the least in number and simplest in composition. By having the furnace acid lined and sanded on the banks, the necessary silica for the slag is available during melting and refining.

Steel Charge

9. The steel charge is chosen with regard for the average sulphur and phosphorus composition and held to less than 0.04 per cent of each. The carbon, manganese, silicon and other oxidizable elements would, of necessity, have to be removed by oxidization. Nickel, copper, molybdenum and other non-oxidizable elements must be expected in the final product and the scrap so selected. Iron ore or some other form of iron oxide is used which, with the sand of the banks, will produce a slag capable of oxidizing the silicon, manganese, carbon and other oxidizable elements of the bath.

Oxidation

10. Having once produced a boiling action by oxidation of the carbon, it is a simple matter to retain the boil with additions of iron ore until the carbon, silicon and manganese have reached a low value. Some feel that a low value of silicon (less than 0.05 per cent) should be strived for, while others use the low point in carbon (less than 0.10 per cent) as their criterion of metal oxidation. In either case, it has been generally proved that the degree of metal oxidation is of marked importance at this stage of refining. Whether the lowered values of residual alloys at this point are the important factor, or whether, by virtue of having oxidized the metal, a prolonged boil is insured is of secondary importance to the fact that sounder steel, having better physical properties, generally results from steel heavily oxidized at some point in refining.

Carbon Addition

11. With the steel in its nearest approach to pure iron, the necessary carbon is added through the medium of a low silicon-low manganese pig iron of approximately 4 per cent carbon. By

adding the pig slowly, the boil of the bath is retained with gradually diminishing intensity until the carbon of the bath and the silica of the slag are of the desired percentage. When the proper temperature has been attained, the power is shut off, the ferroalloys added and the heat tapped.

Carbon and Silica Content Readings

12. After subsidence of the first boil (low point of carbon) and during the final refining, both the carbon content of the bath and the silica content of the slag are obtained by use of the carbanalyzer and viscosimeter. The same procedure of oxidization is followed on all grades of steel but with different expected slag analyses and viscosimeter readings.

METHODS OF ADDING ALUMINUM

13. The heat of steel having been properly made, it is of little consequence where or how the aluminum is added, providing care is taken to insure time for proper dispersion and sufficient aluminum is added to leave an unoxidized residual. Addition to shanks or bull ladles is generally unsatisfactory because of the lack of the necessary time for dispersion. Generally one of three methods of addition is followed.

14. After the ferroalloys are added, the aluminum is sometimes thrust beneath the slag and into the metal prior to tapping. Because of its low specific gravity, it is essential that the aluminum be held mechanically from rising, before solution, into the oxidizing slag which is generally accomplished by firmly wiring the aluminum to a steel rod. This method of addition is somewhat inconvenient and risky as it is necessary to purchase a shape suitable for wiring, and, even when care is exercised, the aluminum will frequently free itself from the rod and rise to the surface under the slag where it may or may not dissolve into the steel. As might be readily expected, addition at this point is attended with greater loss due to high temperatures and maximum interface of oxidizing slag and metal.

15. The most common method of aluminum addition is to the ladle at tapping. Generally, the aluminum is wired to a rod and plunged into the steel. However, it has been found that if the tapped metal is more or less free of slag during the beginning of the tap, the aluminum can be thrown into the flowing stream with almost immediate solution resulting.

16. The third method is addition where lip pour ladles are used

at the pouring stand after slagging off the ladle. By use of a bull ladle, a rich aluminum iron alloy can be produced when the liquid steel is mixed with the aluminum. The aluminum rich alloy can then be poured back into the tapping ladle and thoroughly mixed by subsequent pouring out and pouring back. The liquid steel will readily dissolve approximately one per cent of aluminum, and, generally, two or three bull ladle mixings are sufficient to add the desired aluminum. The advantages of this method of addition are lessened likelihood of aluminum loss through oxidation, and assured dispersion.

17. The present practice of aluminum addition is as given in the third method. When bottom pour ladles are used, the aluminum is added in the ladle at tapping by tossing into the metal stream.

TYPES OF ALUMINUM USED

18. The types of aluminum employed vary both in physical form and chemical purity. When choosing the shapes and size, it is advisable to procure sufficient area of exposure per unit of weight commensurate with rapid solution. Since aluminum prices have been set, all special shapes are sold at a premium, and it will generally be found that a standard notch ingot bar is satisfactory for use. Either primary or secondary aluminum is satisfactory, except for the fact that the more impure grades of aluminum do not dissolve readily. When the aluminum contains greater than 1.5 per cent zinc or magnesium, the solution is somewhat more difficult, probably because volatile zinc or magnesium vapors prevent intimate contact between the aluminum and steel. When the magnesium is higher than 1.5 per cent, considerable sputtering, glare and vapor are experienced. No trouble has been encountered with the use of the impure aluminum other than that mentioned. Table 1 gives the analysis of an impure secondary aluminum which is being used successfully.

Table 1

ANALYSIS OF SECONDARY ALUMINUM

	Per Cent
Zinc	**2.0 to 2.5**
Magnesium	**1.75 to 2.25**
Copper	**5.0 to 7.0**
Iron	**1.5 to 2.5**
Silicon	**0.75 to 1.25**
Manganese	**0.30 to 0.50**
Aluminum	**Balance**

EFFECT OF ALUMINUM ADDITIONS ON CASTING PROPERTIES

19. The only noticeable effect on casting properties of the high aluminum treated steel is experienced in solidification shrinkage. With no surface or internal porosity, the total shrinkage of a thoroughly deoxidized steel must be dealt with. Experience in making vacuum fusion gas analyses on steels treated with aluminum has shown that the aluminum treated steel is more reluctant to release its gases than are the non-aluminum treated steels. Generally, larger partial vacuum or time is necessary for removal of the gases from the aluminum treated steels. This experience probably explains the greater tendency in aluminum treated steel castings toward the frequent surface ruptures or collapses which are manifestations of shrinkage zones.

20. If points of shrinkage within the casting are not properly risered so that they may remain free from the tendency to form shrinkage voids, one or all of three factors will work to establish equilibrium between the external atmospheric pressure and the internal void. Gases may come out of the solidifying steel to fill the void, the weak shell of solidified steel will collapse, or the shell will puncture at its weakest point. Examples of the surface

FIG. 1—EXAMPLE OF SURFACE PUNCTURE AND COLLAPSE IN AN ALUMINUM TREATED STEEL CASTING.

puncture and collapse are shown in Fig. 1. No apparent changes in metal flowability, hot tearing characteristics or penetration have been noted when using high aluminum treated steels.

PHYSICAL TESTS

21. Physical test bars are poured at any time during the casting process in either dry or green sand. The majority of the test bars are poured in double-legged keel blocks made up in dry sand cores. Such cores are easy to keep in stock and are readily rammed into the mold or placed about the pouring floor at various points of pouring.

22. Figure 2 is taken from the paper by Sims and Dahle[1]. A review of the figure indicates the importance of sulphur control. High aluminum treated steel containing 0.035 per cent sulphur or less will have better than 90 per cent of the ductility experienced on the same steel without aluminum. The steels covered in this report contained 0.028 to 0.038 per cent sulphur and generally gave the plus 90 per cent value. With the exception of steels believed to have had unfavorable amounts of hydrogen, the physical properties of the aluminum treated heats have been most satisfactory. Table 2 gives average reduction of area obtained from carbon steel in two carbon ranges.

Table 2

AVERAGE REDUCTION OF AREA OBTAINED FROM CARBON STEEL IN TWO CARBON RANGES

Reduction of Area, per cent	*Carbon Steel—0.20 to 0.25 per cent Carbon* — *Normalized, per cent*	*Normalized and Tempered, per cent*
Under 40	6.0	2.6
40 to 45	30.6	13.3
45 to 50	46.0	40.0
Over 50	17.3	44.0
	Average 150 heats	Average 150 heats

Reduction of Area, per cent	*Carbon Steel—0.26 to 0.30 per cent Carbon* — *Normalized, per cent*	*Normalized and Tempered, per cent*
Under 40	32.0	8.6
40 to 45	48.6	44.0
45 to 50	18.0	33.3
Over 50	1.3	14.0
	Average 150 heats	Average 150 heats

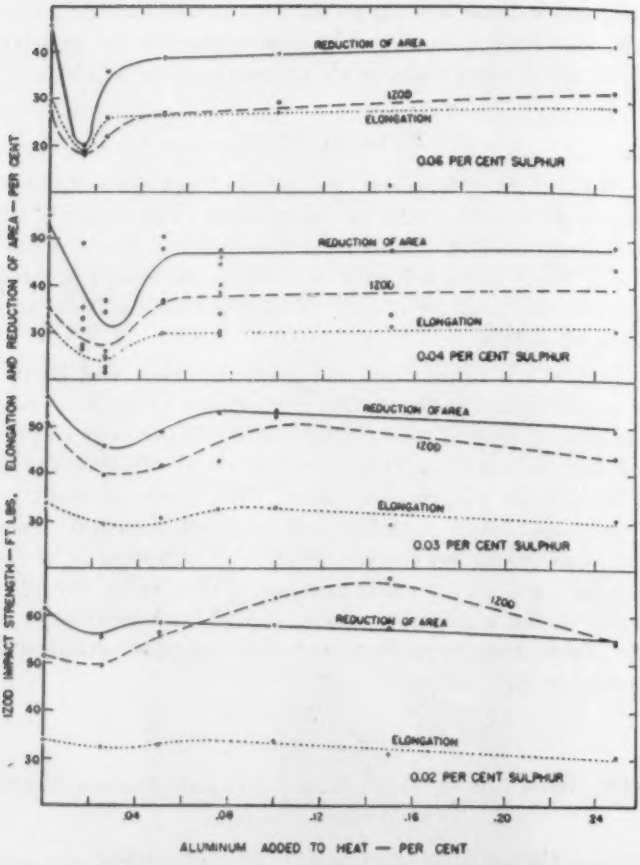

FIG. 2—EFFECT OF ALUMINUM ON DUCTILITY AND IMPACT STRENGTH.

23. On numerous occasions poor ductility has been experienced. Microsamples gave no indication of the cause. Because this condition was found to be more prevalent on heats of steel given little opportunity to boil and on test specimens cast in exceptionally damp sand, and because tempering at temperatures as low as 700°F. (371°C.) would generally improve the ductility, blame for these poor showings was laid to hydrogen. Tension bars having apparent hydrogen contamination failed with V-shaped flaws on the bar surface and bright spots in the fracture. Test bars receiving a normalizing treatment only gave poorer results than bars tempered after normalizing. In a few cases, bars normalized and tempered gave exceptionally low values. By tempering a second

time, the ductility was almost always measurably improved. By normalizing is meant heating to 1600°F. to 1700°F. (871°C. to 927°C.), holding for not less than one hour per in. maximum cross section and then cooling in still air. The tempering operation spoken of was carried on by heating to 1100°F. to 1200°F. (593°C. to 649°C.), holding for one hour per in. thickest section and cooling in still air. In all cases where ductility was brought back to normal by tempering, the V-shaped flaws and bright spots disappeared.

24. The writer has suggested that this loss in ductility which is curable by tempering treatment be credited to hydrogen. In a paper by Zapffe and Sims[2], a most satisfactory explanation credits conditions described above to hydrogen. The authors point out that prevention of hydrogen absorption during the making of the steel is often impracticable. It is obvious that pouring of the metal in molding sands, especially green, offers excellent opportunity for hydrogen contamination. Since it is impracticable to prevent all hydrogen absorption, it becomes essential to lower the hydrogen concentration, if it becomes objectionably high, by some method such as heat treatment.

25. Zapffe and Sims reasoned that—"Theoretically, annealing would be most efficient at some elevated temperature that lies below the critical temperature for embrittlement above which the solubility, decreasing on cooling, would cause hyper-elastic aerostatic stress to develop." This temperature was chosen at just below 1401°F. (725°C.). As a check on their reasoning, the authors heated mild steel specimens in hydrogen to 2044°F. (1100°C.), held them 3 hours and then quenched them in water to insure considerable retention. The specimens were then heated for varying periods of time at temperatures of 212°F. to 1472°F. (100°C. to 800°C.) in 212°F. (100°C.) intervals and then quenched in water. Tensile specimens were taken and the reduction of area plotted for various temperatures against time, as shown in Fig. 3.

26. Examination of the fractures of the test bars indicated steadily decreasing hydrogen embrittlement as time or temperature was increased, as evidenced by smaller and fewer bright spots. Table 3 shows tensile test results on four test bars from the same heat of steel. Bars 1 and 2 had no aluminum addition, while bars

[2] Zapffe, C. A., and Sims, C. E., *"Hydrogen Embrittlement, Internal Stress and Defects in Steel,"* TRANSACTIONS, Iron and Steel Division, American Institute of Mining and Metallurgical Engineers, vol. 145 (1941), pp. 225-271.

3 and 4 were treated with 0.15 per cent aluminum. Bars 1 and 3 were normalized only, while bars 2 and 4 were tempered after normalizing. The striking improvement in the ductility of the aluminum treated steel after tempering is accredited to the removal of hydrogen as explained by Zapffe and Sims.

27. It has been the writer's experience that steels treated with aluminum are more prone to this condition of hydrogen contamination than are steels not treated with aluminum. For example, consideration must be given to the observed difference in etching effect on the aluminum treated and non-aluminum treated steels.

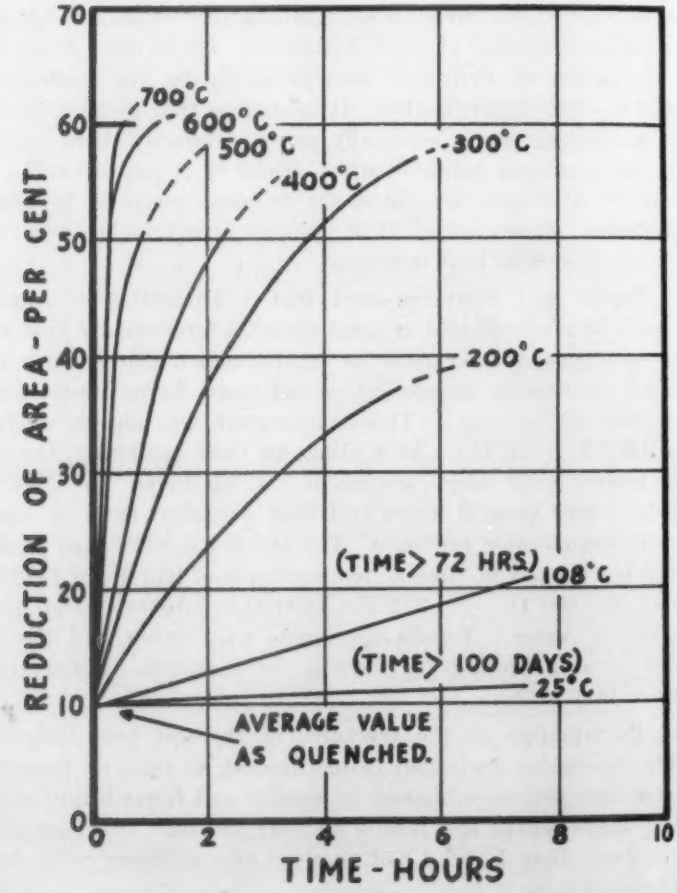

FIG. 3—DUCTILITY OF MILD STEEL SPECIMEN AS MEASURED BY PERCENTAGE OF REDUCTION OF AREA, SHOWING RELATIONSHIP WITH TEMPERATURE AND PERIOD OF ANNEAL.

Shortly after starting to make the high aluminum additions to the steel, it was noted that excessive etching occurred when micro-samples were prepared under standard technique. While it cannot be claimed that this effect is related to hydrogen, yet it is worth noting that tempered specimens were less abnormal than were specimens normalized only.

Table 3

TENSILE TEST RESULTS ON FOUR TEST BARS FROM SAME HEAT OF STEEL

	Normalized Only		*Normalized and Tempered*	
	Without Aluminum, Bar 1	*With Aluminum, Bar 3*	*Without Aluminum, Bar 2*	*With Aluminum, Bar 4*
Tensile Strength, lb. per sq. in.	74,000	73,700	70,100	70,250
Yield Point, lb. per sq. in.	43,200	46,050	41,100	42,300
Elongation, per cent in 2-in.	27.0	21.0	30.5	30.0
Reduction of Area, per cent	40.0	28.1	47.4	48.4

28. As a rough indication of the rate of 1:1 hydrochloric acid attack, two samples machined to the same size were etched for 24 hours in the same beaker with 1:1 hydrochloric acid at room temperature. Both specimens were normalized and from the same heat of steel. The aluminum treated specimen had a loss in weight of 1.86 per cent, while the non-aluminum treated specimen lost only 1.50 per cent of its weight. It is not intended that these rough tests be indicative of anything other than confirmation of visual observations of differences in etching rates.

CALCIUM-SILICON ADDITIONS

29. Numerous persons have suggested the use of calcium-silicon alloy with the use of high aluminum. Generally, it is recommended that the calcium-silicon be added to the ladle prior to the addition of the aluminum. While no great number of heats was made with calcium-silicon additions, enough testing was carried out to satisfy the writer that little was to be gained from its use, providing the steel had been properly made beforehand. The tests run, using 0.05 per cent calcium addition, prior to aluminum, indicated no marked improvement in tensile or casting properties.

30. Since the addition of calcium-silicon results in a great deal of agitation from apparent volatilization of the metallic calcium,

it would seem reasonable that the agitation might help the removal of excessive hydrogen if it were present in the steel. If the calcium is added after the aluminum, little is claimed for its use. Assuming the removal of hydrogen to be its primary function, it is possible that the hydrogen would be less easily shaken loose once the aluminum is added. Claim has been made for smoother, cleaner castings from calcium treated metal. At no time has the writer experienced any improvement of this nature.

EFFECT OF ALUMINUM ADDITIONS ON GRAIN GROWTH

31. As might be expected, the aluminum treated steels are very resistant to grain growth at temperatures below 1900°F. (1038°C.). On valve work, where high creep resistance is required, some engineers feel that it is advisable to have a coarse grained steel. The writer has found that he is unable to accomplish any marked grain coarsening by heating for prolonged periods at temperatures up to 1950°F. (1066°C.). At 2000°F. (1093°C.), a somewhat mixed grain is obtainable. However, unreasonable scaling forbids annealing temperatures and times necessary to produce this condition.

32. Recent data suggest that creep is more a function of pearlite structure than grain size, so, perhaps the coarse grained steels are not necessary. Dr. H. W. Gillett, chief technical advisor of Battelle Memorial Institute, recently reported[3] on some tests which indicated a difference in grain coarsening temperature for high aluminum steels with varying sulphur content. The data indicated that the coarsening temperature was lower for lower sulphur content.

POROSITY

33. During the period covered, only three heats of steel have been lost because of porosity. Two of these heats did not receive their aluminum addition, and pin holes were experienced. The third heat was 0.09 per cent carbon, 0.15 per cent manganese, 2.00 per cent silicon steel, treated with 0.15 per cent aluminum. The melter, feeling secure because of the 2.00 per cent silicon, neglected to properly boil his metal and the castings showed an extreme case of pin holes. Several isolated cases of castings having porous areas have been experienced. Invariably

[3] Gillett, H. W., *"Aluminum-Sulphur Relations in Steel,"* Sauveur Lecture, American Society for Metals, 1942.

these could be traced to wet sand or wet areas of the mold caused by water spraying to aid mold finishing.

LOW DUCTILITY

34. While several heats have shown unusually low ductility on first test, retempering has generally brought the ductility back to normal. On four occasions, the ductility was not improved by heat treating. Investigation showed the stringy type of inclusion. It was assumed that the residual aluminum was too low, probably due to excessive oxidation. Greater care during addition of the aluminum easily remedies a failure due to this cause.

ADVANTAGES AND DISADVANTAGES

35. The following advantages and disadvantages have been realized from usage of the high aluminum additions:

Advantages:

(1) Sounder castings with less likelihood of periodic pin hole trouble.
(2) More uniform tensile test results with few failures in ductility values.
(3) Lessened strain on the foundry personnel due to both of the above.

Disadvantages:

(1) Shrinkage cavities generally larger due to lack of other porosity which when present accounts for part of the shrinkage.
(2) Coarse grained structure not obtainable.
(3) Probably more likelihood of hydrogen contamination or retention.
(4) Cost of added aluminum.

36. It is hoped that all of the disadvantages experienced when using aluminum will eventually be remedied. The high aluminum treatment is, at least, a partial answer to the problem and, as such, has been of immeasurable aid in the manufacture of better steel castings.

DISCUSSION

Presiding: C. W. BRIGGS, Steel Founders' Society of America, Cleveland, O.

MEMBER: Is the viscosimeter used generally in your work, and can the reading be correlated with the amount of aluminum to be used? Also, can the manganese returns consistently be held within 20 points, or 60 to 80, in a numerical specification?

MR. TROY: We use the viscosimeter on all heats; we do not fluctuate the aluminum additions with fluctuating viscosimeter readings; rather we attempt to adjust to uniform slag analysis and fix the aluminum addition. The purpose of the viscosimeter is to control the steelmaking, not to adjust with aluminum for discrepancies in steelmaking. We manage on manganese under 0.90 per cent to hold approximately 95 per cent of the heats to a 20 point manganese range.

MEMBER: Do you use your viscosimeter to correlate your manganese efficiencies? You mentioned getting 90 to 92 per cent. That is quite true, but in some recent work done along that line it would show anywhere from about 50 per cent to as high as 70 and 80 per cent efficiencies. On some of the inspections there is a maximum of 80 to 85 per cent. Sometimes that seems rather difficult to control, and I just wonder whether anybody else has had the same experience.

MR. TROY: The viscosimeter is used to determine the silicate degree of the slag. Adjustments to the slag are made to bring about control of basic oxides (iron and manganese). By controling the chemistry of the slag, part control over manganese loss is possible; the quantity of slag, as well as quality, must be regulated for absolute manganese control. Eliminating the slag just before the addition of manganese is the best means of getting consistent, maximum manganese recoveries. By carefully removing the slag, leaving only a very thin slag film, prior to the addition of ferromanganese, consistent recoveries of 90 to 95 per cent of the added manganese are possible.

L. B. POLEN[1]: Do you continue to use calcium silicon with the alsifer? If so, at what point in the tapping procedure did you add it?

MR. TROY: We do not use calcium silicon. We did, at one time, experiment with additions of calcium silicon of both 0.05 per cent and 0.10 per cent calcium to the ladle before the aluminum. We have never used calcium silicon in conjunction with alsifer. Our experience with the calcium silicon indicated that its questionable advantages did not offset its cost. While some shops whole-heartedly endorse its use, we were unable to see any marked improvement when added to well-boiled steel. It is worth notice that calcium-silicon additions to the ladle result in considerable agitation with apparent voltilization of calcium vapors. It seems quite possible that such agitation could result in liberation of hydrogen from a hydrogen-contaminated heat.

WILLIAM MORLEY[2]: I would like to ask if you made any examination, through the microscope, of these white spots in the fracture of tensile bars.

[1] Fort Pitt Steel Casting Co., McKeesport, Pa.
[2] Link Belt Co., Chicago, Ill.

MR. TROY: We have examined these areas without arriving at any conclusions. The best data on this subject will be found in the papers of Zapffe and Sims on hydrogen. We have experienced more difficulty with bend tests than tensile tests. A number of bends have failed prematurely by starting at an edge fissure or crack. In two cases this fissure was discovered before making the bend by etching the bend bar. Close examination of the fissure before and after rupture showed a type of interdendritic fissuring, as explained by Zapffe and Sims* in their work on hydrogen. Efforts were made to induce this fissure by changes in test bar design. The fissure turned up only occasionally, and no method was found to insure its formation. This trouble was rarely encountered on green or baked resin sands and often encountered when using baked oil sands. Venting of the baked sands markedly reduced the frequence of encounter.

MEMBER: Supposing you have approximately an 80,000-lb. ladle. What mechanical method would you use to add the aluminum to the ladle? Say that you had poured half of the charge, by what method would you get the aluminum through the slag?

MR. TROY: We have never poured an 80,000-lb. heat. I assume that if properly protected two men might plunge the aluminum, if it were wired to a heavy rod.

MEMBER: Would the aluminum at that time have anything to do with the shrinkage? In other words, would we have a greater shrinkage with the aluminum or without it?

MR. TROY: More problems from shrinkage, not greater shrinkage, have been experienced. Liquid shrinkage makes itself more apparent when all surface porosity is eliminated. When pinholes are experienced, a part of the shrinkage goes into making up the porous voids.

VICTOR E. ZANG[2]: I am wondering if you are having trouble with variations in scrap, in sulphur contents of scrap, and if your set aluminum addition handles those variations that might come up, or if you vary your aluminum content to take care of such variance.

MR. TROY: We are fortunate in having uniform scrap. As explained by Sims and Dahle[1], the addition of alumium in increasing quantities will not offset increasing sulphur. The quantity, shape and location of the sulphide inclusions determines the ductility. As the sulphur increases the ductility decreases.

CHAIRMAN BRIGGS: Mr. Troy, could you add anything regarding the analysis of residual aluminum, such as, when you do it, is it an occasional check and do you try to hold to some threshold value of residual aluminum?

MR. TROY: We have made a few attempts at residual aluminum. None gave us any feeling of satisfaction. The best test for sufficient aluminum is the absence of trouble. Either low ductility or pinholes are ample evidence of something wrong. Examination of inclusion type will indicate the presence or lack of sufficient aluminum.

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* For reference, see page 811.
[2] Unitcast Corp., Toledo, Ohio.

F. A. MELMOTH[4]: Assuming the chemistry of the acid open hearth to be almost identical with the chemistry of the electric furnace, and referring simply to chemistry, can the author of this paper tell me why we do not have to use this aluminum in the acid open hearth? The tendency to contamination in the making of castings is the same. In fact, I might go a little further and say it is even more, at times, because of the great number of open-hearth castings of the type that are made with green sand and green sand cores. They are large and rangy. The steel has every opportunity for hydrogen contamination, and yet, either with no aluminum addition at all or a very, very small amount, it possesses a singular freedom from porosity. A more important matter is that we rarely find the stringy type of inclusion. A study of the test books of acid open-hearth foundries will convince you that one very rarely finds any serious test-bar trouble. It has come to be accepted that the acid open hearth will give test bars that have 40 to 50 per cent reduction of area every time, and we do not argue aluminum, and its effects, as with acid electric. Is it possible that temperature variations are much more critical than we have believed?

Basing it on this, are we attempting to find a sort of panacea rather than attacking the fundamentals? A fundamental that is in the electric process is the taking down to low carbon. Can Mr. Troy tell me whether, if he fails to go down to low carbon, he gets this influence of heavy additions of aluminum and, if not, why not? Let us assume the steel has had a very satisfactory boil and that any mechanical removal of hydrogen has taken place—in other words, the heat was started rather high in carbon and was taken down, perhaps, to 0.20 per cent carbon rather than the 0.10 per cent which has been referred to so frequently—does he get the same type of result? Is it precisely the same kind of action in those conditions, or it is possible that the boil in the open hearth, which is maintained for a very much longer time per unit ton, has some physical action on some critical amount of hydrogen? We do not know what that critical amount is, we have nothing tangible to go on, and I am still perplexed as to why acid steel out of the open hearth does not warrant all of this "tinkering with."

MR. TROY: I believe there are answers to some of these questions. Well made acid electric steel with aluminum additions, no greater than in acid open-hearth steel, can be poured into the larger castings without pinhole troubles. Without aluminum, well-made acid electric steel will give ductility equal to acid open-hearth steel. Pinholes are prone to occur in the smaller green sand castings where molds are quickly closed and poured. Squeezer molds are generally poured in 5 to 10 sec., while larger molds run up in pouring time to as long as 3 min. Because the larger molds require longer finishing time prior to closing, and because the larger mold is in part preheated during pouring, the mold face of the larger mold is likely to be drier. I agree that the open hearth produces more consistent metal. The open-hearth steel is longer in the making and is assured a strong, uniform boiling period.

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[4] Detroit Steel Casting Co., Detroit, Mich.

Boiling is necessary to temperature in the open hearth, but not in the electric. Too many electric melters just melt. As long as a good boil up to the period of tapping is secured, the carbon range seems to make little difference. Steel boiled from 0.40 to 0.20 per cent carbon gives equal results to steel boiled down to 0.10 per cent carbon, providing the boil be continued up to additions of ferrosilicon and manganese. The use of aluminum as a deoxidizer need not be considered a panacea any more than silicon.

If it were not for the effect of aluminum on the sulphide and resultant effect on ductility, there would be no question regarding its use as a deoxidizer. Aluminum is a better deoxidizer than silicon, but must be more carefully used.

W. A. PARKER[5]: You said it was a question of the size or the weight of the castings, in comparing the open hearth with the electric furnace. I cannot agree with you there. We have poured just as small castings from the open hearth as we ever poured from the electric, without the use of aluminum and, as Mr. Melmoth said, I do not know why acid steel out of the open hearth does not require all of this "tinkering with."

MR. TROY: I do not know why either. There should be a sense of relativity in this whole discussion. On small casting even slight porosity results in scrap, while the same degree of porosity in large castings is not noticed.

MR. MELMOTH: Mr. Parker just reminded me of something. Long before the war, we both were making brake shoes. They weighed 11 lb. and were very thin. If we were not extremely careful in the electric furnace, and used aluminum, we had many failures on account of porosity. If I remember rightly, Mr. Parker was pouring them from an open-hearth furnace, with no aluminum at all, and was not having any trouble.

Now, you cannot talk about relative this and relative that when those were the same casting. There was no question of relativity and there was no favoritism. They were making them without pinholes, but in the electric we got them.

MR. TROY: There is still the question of sand practice when discussing pinholes. In borderline cases we have found that soft ramming, coarse sands and high permeabilities are more conducive to pinholes than fine, tightly-rammed sand. While these findings are contrary to most published fact, they have, nevertheless, been witnessed numerous times. If close examination of pinholes is made, it will generally be found that the greatest tendency for pinholes occurs in the vicinity of rough casting surface. We have made experiments with simple block patterns. One side of the pattern was rammed exceedingly hard, while the other side was soft-rammed. Acid electric steel without aluminum was poured into the mold. The hard-rammed side had no pinholes, while the soft-rammed side was badly pitted with typical pinholes.

H. D. PHILLIPS[6]: On this question of pinhole porosity in open hearth versus electric, we have found in our plant (of course, we do not have

[5] Continental Roll and Steel Foundry Company, East Chicago, Ind.
[6] Lebanon Steel Foundry, Lebanon, Pa.

an open-hearth plant) that using electric steel and employing the same melting practice at all times, if we vary our pouring temperature, we can easily eliminate porosity in steel without the use of aluminum; that is, if we keep the pouring temperature way down. I think that is the biggest thing there. I am quite certain that this has more to do with it than anything else, assuming that you make a heat of steel in the electric furnace as you should make it, as Mr. Troy described in this paper. This procedure is almost sure to deliver into the ladle a metal with a very low gas content. If you pour at a temperature which is consistent with the proper running of the mold and no more, your chances for porosity are a lot less than when you pour it hot, and I am quite sure that there are open-hearth furnaces handling heats in the bottom-pour ladle at much lower temperatures that you normally encounter in the electric shops. We have run for a long time without the use of any aluminum at all in bottom-pour heats, in green sand molds, without experiencing any trouble with porosity, but occasionally we will get off on temperature, principally because a man reads the pyrometer incorrectly, and then porosity results.

Effects of Various Elements on the Mechanical Properties and Dezincification of Manganese Bronze‡

The Effects of Lead and Tin on the Mechanical Properties and of Arsenic, Antimony, Phosphorus, Lead and Tin on the Dezincification of Manganese Bronze

By Alfred H. Hesse†, Lake Charles, La., Edwin T. Myskowski* and Blake M. Loring*, Washington, D. C.

Abstract

The effect of lead and other elements on manganese bronze has been studied in detail, especially in regard to the changes in mechanical properties and resistance to dezincification that may result from furnace charges of contaminated scrap metal. There appears to be little tendency for lead segregation in a well-made manganese bronze. More lead may be permitted if the copper-zinc ratio and the tin content are carefully controlled.

Tin appears to be one of the best inhibitors of dezincification. Phosphorus, arsenic and lead are in general effective to a certain extent. Antimony not only decreases ductility seriously but also tends to increase the dezincification.

Introduction

1. Manganese bronze is an important foundry alloy of the approximate composition of 55 to 60 per cent copper, 35 to 40 per cent zinc, and zero to about 2 per cent each of aluminum, iron, manganese and tin. At present, this composition is attracting much attention as an outlet for many types of scrap and as a substitute for the increasingly scarce tin bronzes. Many difficulties have arisen when lead contaminated scrap has found its way into the manganese bronze. Lead, being insoluble in copper alloys, causes

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† Mathieson Alkali Works Inc., Magnesium Plant.
* Naval Research Laboratory, Anacostia Station.
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lower tensile strength, elongation and impact resistance. But manganese bronze owes its value as a non-ferrous casting alloy to the fact that it is as strong as mild steel and has the added advantage of high corrosion resistance.

2. The major objective of the present investigation was to find out how much lead might be tolerated in various compositions of manganese bronze without harm to the mechanical properties. Since the investigation was to be carried out on compositions with varying percentages of tin (a corrosion inhibiting element), it was necessary to study the corrosion resistance as well as the mechanical properties. In this connection, the investigation also included the effects on corrosion resistance of small percentages of arsenic, antimony, phosphorus and lead.

EFFECT OF LEAD AND TIN ON MECHANICAL PROPERTIES

3. Little has been published on the mechanical properties of sand cast manganese bronze, although this is not true of the wrought and chill cast alloys. Ellis[1] found that lead, especially in the range 0 to 0.25 per cent, markedly decreased the tensile strength, elongation and reduction of area. Earlier investigators found lead to be harmful to the tensile properties of 70/30 and 60/40 brasses. However, all investigators are not in agreement in regard to the harmfulness of lead. This undoubtedly is explained by the wide spread in physical properties of manganese bronze which accompanies a variation in the ratio of copper to zinc.

4. In his discussion of the importance of the copper-zinc ratio, Guillet[2] has shown that iron, aluminum, manganese and tin are equivalent to certain percentages of zinc in their effect on manganese bronze, and that nickel is equivalent to a certain percentage of copper. Table 1 gives the zinc and copper equivalents, according to Guillet. By addition of the respective equivalent percentages the total equivalents of copper and zinc are obtained. Then,

$$\frac{\text{total equivalent per cent copper}}{\text{total equivalent per cent zinc}} = \text{Cu-Zn ratio}^*$$

[1] Superior numbers refer to references at end of paper.

* NOTE: In the present investigation, lead was not considered in computing the copper-zinc ratio because it is almost insoluble in copper alloys.

Table 1

Zinc and Copper Equivalents

Element	*Equivalent*
Copper	1 x % Cu = Cu
Nickel	1.1 x % Ni = Cu
Zinc	1 x % Zn = Zn
Aluminum	6 x % Al = Zn
Iron	0.9 x % Fe = Zn
Manganese	0.5 x % Mn = Zn
Tin	2 x % Sn = Zn

Effects of Several Elements on Properties

5. Important as the copper-zinc ratio is in its influence on the mechanical properties of manganese bronze, there are many significant effects contributed by the individual elements depending to a considerable extent on the particular percentages. For instance, manganese improves the ductility if not in excess of 2 per cent. Iron also improves the ductility, and both manganese and iron within certain limits cause moderate increases in tensile strength. On the other hand, tin, which produces a slight increase in tensile strength, must be added in limited percentages as an inhibitor of corrosion because in larger amounts it becomes harmful to ductility. Aluminum, likewise detrimental to ductility, is the major element for increasing the tensile strength of manganese bronze. The metallurgical basis for the production of manganese bronze thus depends on balancing the above elements in order to have an alloy with an optimum combination of tensile strength, ductility and corrosion resistance.

Influence of Lead and Tin

6. *Experimental Procedure.* The following nominal composition was selected as representative:

59.0 per cent copper
38.5 per cent zinc
1.0 per cent iron
0.5 per cent manganese
1.0 per cent aluminum

The alloys were made from grade A copper, electrolytic zinc and master alloys of high purity. Two-hundred lb. charges were melted in an induction furnace under a charcoal cover in a graphite

crucible. The copper was melted first, and the iron and aluminum added in the form of a 50/50 master alloy melting at approximately 1150°C. (2100°F.). Following this addition, the manganese was put in as copper manganese and the zinc added later as virgin metal. Forty-lb. heats of the above composition were then melted in an induction furnace and the tin and lead added with appropriate adjustments of the copper and zinc for each copper-zinc ratio.

7. All melts were deoxidized with one oz. of 15 per cent phosphor-copper per 100 lb. of metal before pouring at 980 ± 10°C. (1795 ± 20°F.) into bottom-fed "separately cast bar" tensile molds prepared in accordance with N. D. specification 49-B-3e. The molds, which were made of No. 00 Albany sand containing about 6 per cent moisture, were air dried at least 24 hr. before using. The "separately cast bar" castings provided sufficient metal for two standard 0.505-in. tensile bars, Izod impact specimens, chemical samples, micrographic specimens, and sea water corrosion test plates.

8. *Pouring Temperature.* Heat 194 was cast into five "separately cast bar" molds at intervals of 25°C. (45°F.) beginning at 1025°C. (1875°F.) and ending at 930°C. (1705°F.) to determine the influence of pouring temperature. In all instances, pouring

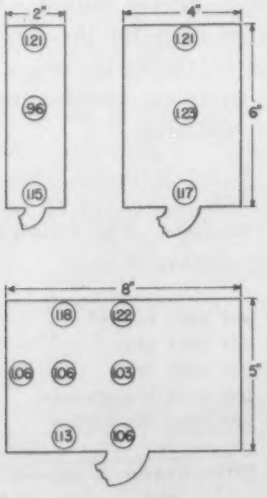

FIG. 1—LEAD DISTRIBUTION IN MANGANESE BRONZE CYLINDER CASTINGS. ENCIRCLED NUMBERS SHOW PER CENT LEAD.

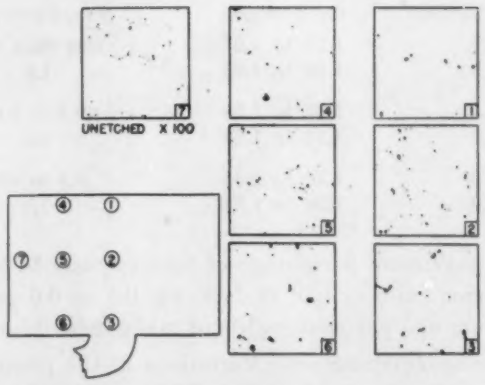

FIG. 2—UNETCHED PHOTOMICROGRAPH SHOWING LEAD DISTRIBUTION IN 8-IN. MANGANESE BRONZE CYLINDER CASTING. TAKEN FROM CORRESPONDING POSITIONS OF 8-IN. CASTING SHOWN IN FIG. 1.

temperatures were determined by means of an immersion type platinum platinum-rhodium thermocouple which was calibrated at the necessary temperatures against a standard couple.

9. *Lead Segregation.* For the study of lead distribution, manganese bronze alloys containing nominally 1.0 per cent lead, melted in a gas-fired furnace under oxidizing conditions and deoxidized with phosphor-copper following standardized procedures, were poured into bottom-fed horn-gated molds to produce solid cylinders, 2, 4, and 8-in. in diameter. The cylindrical castings were cut longitudinally through the diameter so that chemical and micrographic samples might be selected from the inside surface (Figs. 1 and 2).

Discussion

10. *Effect of Copper-Zinc Ratio on the Permissible Percentage of Lead.* From the results of the present work plotted in Figs. 3 and 4, it appears that the low tensile strength and elongation caused by lead may be compensated for by adding tin and controlling the copper-zinc ratio. This balance of properties is made possible by the fact that a high percentage of tin and a low copper-zinc ratio contribute to high tensile strength and low elongation, while low tin and a high copper-zinc ratio have the opposite effect. The minimum specification requirements are represented in Figs. 3 and 4 by horizontal lines and the conditions under which these minimum requirements are met may be summarized as follows:

Lead, Per Cent	*Cu-Zn Ratio*	*Tin, Per Cent*
0.7	1.20 to 1.25	less than 0.6
0.0	1.20 to 1.25	1.0
1.5	1.26 to 1.28	0.3 to 0.6
1.0	1.26 to 1.28	0.0
1.0	1.30 to 1.34	0.4 to 0.6
0.0	1.30 to 1.34	0.0

11. The maximum percentage of lead appears to be possible if the copper-zinc ratio is 1.26 to 1.28, tin 0.3 to 0.6 per cent, iron and aluminum one per cent each and manganese 0.5 per cent.

12. *Pouring Temperature.* Variations in the pouring temperature of manganese bronze between 950°C. (1740°F.) and 1025°C. (1880°F.) have no influence on the mechanical properties. A pouring temperature of 930°C. (1705°F.) was too low as shown by the decrease in both tensile strength and elongation.

13. *Effect of Lead.* The effect of from zero to two per cent lead on the tensile strength, elongation and impact strength is shown in Figs. 3, 4, and 5. The data used for the preparation of these plates are given in Tables 2, 3 and 4. The greatest drop in impact strength occurs between zero and 0.5 per cent lead. When

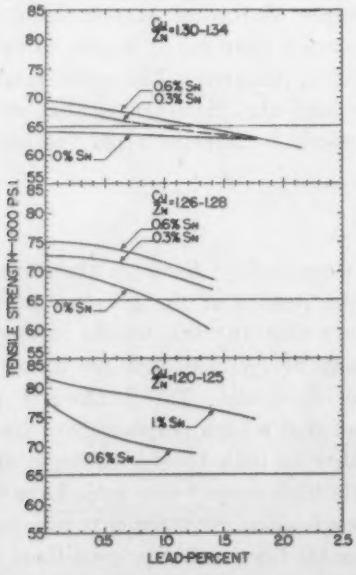

FIG. 3—EFFECT OF LEAD ON TENSILE STRENGTH OF MANGANESE BRONZE.

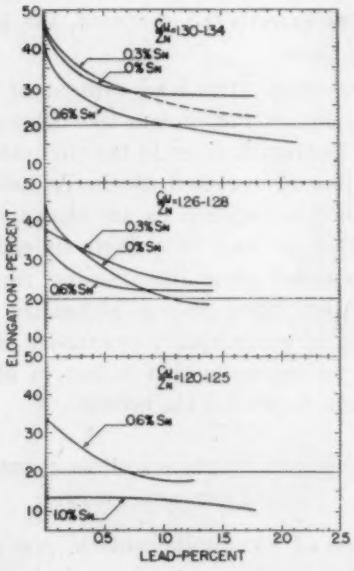

FIG. 4—EFFECT OF LEAD ON ELONGATION OF MANGANESE BRONZE.

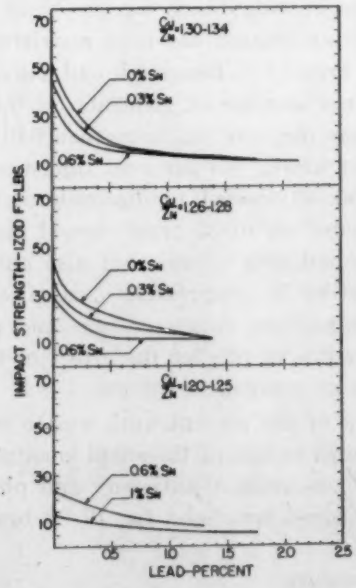

FIG. 5—EFFECT OF LEAD ON IMPACT STRENGTH OF MANGANESE BRONZE.

the amount of lead exceeds 0.5 per cent, the impact strength is lowered but little more.

14. *Lead Segregation.* The lead content at certain points in the 2, 4, and 8-in. diameter cylinders was determined and the per cent lead for each location is given in the encircled numbers in Fig. 1. Photomicrographs of unetched specimens taken from the same positions as the chemical specimens are shown in Fig. 2. No appreciable segregation of lead is evident in either case. This is what might be expected since the freezing range of manganese bronze is very narrow. Thus, since solidification takes place rapidly, the lead which is mechanically suspended does not have the same opportunity to segregate that it has in alloys having wide freezing ranges such as leaded tin bronze.

THE EFFECT OF ARSENIC, ANTIMONY, PHOSPHORUS, LEAD AND TIN ON DEZINCIFICATION

15. The presence of certain elements in cast manganese bronze has a marked effect on its resistance to dezincification when exposed to corrosive media such as chloride, sulfate, and nitrate solutions which tend to cause the selective loss of zinc.

16. In previous investigations, the study of the influence of composition on dezincification has been restricted almost entirely to 70/30 or alpha brass[3, 4, 5]. Bengough and associates investigated the effect of a large number of elements on this type of brass[6]. They found that one per cent tin, more than 0.01 per cent arsenic, 0.5 to one per cent nickel, 0.5 per cent tungsten, and 0.5 to 0.75 per cent aluminum all tended to diminish dezincification. The investigation of Lynes[7] on 70/30 brass showed that small additions (0.03 per cent) of not only arsenic but also antimony and phosphorus were effective in suppressing dezincification. Since the differences in composition, microstructure and properties are so great, it is not possible to predict the effect of these elements on the dezincification of manganese bronze.

17. The purpose of the present work was to evaluate the effectiveness in manganese bronze of the small amounts of inhibitors to dezincification such as arsenic, antimony and phosphorus that already have been proved beneficial for 70/30 brass.

Experimental Procedure

18. *Preparation of Alloys.* The special elements tin, phosphorus

Table 2

EFFECT OF LEAD ON MECHANICAL PROPERTIES OF MANGANESE BRONZE OF 0.9 TO 1.3 PER CENT TIN CONTENT

Composition, Per Cent							Mechanical Properties				
Heat No.	Cu	Zn*	Sn	Fe	Al	Mn	Pb	Yield Strength, lb. per sq. in. (0.1 per cent of gage length)	Tensile Strength, lb. per sq. in.	Elongation, Per Cent in 2-in.	Impact Strength at 21°C., Izod ft.-lb.	Cu-Zn Ratio
187A	57.25	39.26	0.90	0.72	0.89	0.98	0	63,800	81,900	13.3		1.20
187B	57.41	38.67	1.24	0.81	0.87	1.00	0	59,300	62,000	4.7		1.21
187C	58.04	38.03	1.17	0.80	0.92	1.04	0	58,100	73,600	11.7	13.5	1.23
188A	57.38	38.25	1.19	0.78	0.93	0.96	0.51	61,200	79,300	13.3		1.21
188B	57.68	37.90	1.17	0.79	0.94	0.98	0.54	59,000	79,800	16.4	9.0	1.23
189A	58.90	36.07	1.13	1.02	0.90	0.96	1.02	49,800	70,500	19.5		1.31
189B	59.29	35.55	1.29	0.95	0.96	0.98	0.98	47,700	54,600	7.4	9.5	1.31
190A	56.66	37.65	1.31	0.86	0.87	0.99	1.66	59,700	72,600	9.8		1.21
190B	57.13	37.24	1.30	0.81	0.87	1.01	1.64	57,500	70,600	9.4	5.5	1.23
191A	56.64	37.55	1.17	0.89	1.00	0.99	1.76	61,100	77,100	11.7		1.20
191B	56.88	37.10	1.20	1.05	1.00	1.02	1.75	58,000	70,000	8.6	5.5	1.21

* By difference.

Table 3

EFFECT OF LEAD ON MECHANICAL PROPERTIES OF MANGANESE BRONZE OF 0.4 TO 0.6 PER CENT TIN CONTENT

Composition, Per Cent								Mechanical Properties				
Heat No.	Cu	Zn*	Sn	Fe	Al	Mn	Pb	Yield Strength, lb. per sq. in. (0.1 per cent of gage length)	Tensile Strength, lb. per sq. in.	Elongation, Per Cent in 2-in.	Impact Strength at 21°C., Izod ft.-lb.	Cu-Zn Ratio
193	58.30	38.85	0.63	1.02	0.79	0.41	0	43,500	74,000	34.0	25.0	1.27
194	57.93	39.16	0.61	0.98	0.91	0.41	0	47,900	77,700	32.8	19.5	1.23
196	58.48	38.60	0.57	0.92	0.97	0.46	0	45,000	76,000	33.7	24.0	1.25
B-15-1	58.59	38.79	0.51	0.93	0.77	0.41	0	39,700	71,000	42.6	35.0	1.29
B-18-1	59.02	38.17	0.65	0.80	0.90	0.46	0	42,900	73,200	38.6	31.5	1.29
B-30-5	59.34	37.96	0.51	0.86	0.87	0.46	0	39,000	70,000	41.5	35.5	1.31
B-15-2	59.22	38.14	0.53	0.98	0.72	0.41	0	37,600	68,600	42.5	41.0	1.33
B-18-2	57.90	39.18	0.62	0.96	0.88	0.46	0	46,700	77,900	32.9	24.0	1.24
B-30-6	59.19	37.77	0.50	0.86	0.89	0.46	0.33	39,500	69,000	33.8	22.0	1.31
197	58.24	38.47	0.57	0.87	0.96	0.38	0.51	46,000	74,000	23.4	12.0	1.26
198	57.91	38.45	0.59	0.90	0.98	0.45	0.72	45,800	70,500	19.6	11.5	1.24
199	57.85	37.90	0.58	0.93	1.02	0.46	1.26	45,800	68,700	18.0	8.5	1.25
B-9	58.50	38.78	0.64	0.87	0.64	0.48	0.09	40,800	68,900	32.8	30.0	1.30
B-8	58.64	38.77	0.53	0.84	0.66	0.43	0.13	39,600	68,800	32.8	33.5	1.31
B-10	58.49	38.65	0.69	0.86	0.60	0.48	0.23	41,000	69,100	31.3	21.0	1.31
B-11	58.21	38.59	0.79	0.85	0.64	0.49	0.43	41,100	67,700	23.4	16.0	1.30
B-2	58.87	37.89	0.60	0.67	1.00	0.41	0.56	45,000	68,700	19.5	18.0	1.28
B-3	58.97	37.43	0.60	0.61	0.92	0.42	1.05	42,000	65,900	20.3	14.0	1.31
B-4	58.46	37.38	0.60	0.64	0.96	0.43	1.53	42,200	64,400	17.2	12.5	1.29
B-1	59.01	38.39	0.61	0.58	0.99	0.42	0	45,300	68,200	19.5	35.0	1.27
200	57.87	37.70	0.65	0.73	0.84	0.54	1.67	40,000	64,300	18.8	9.0	1.29
B-5	58.13	37.15	0.61	0.60	0.92	0.44	2.15	40,500	61,300	15.2	11.5	1.30
B-46-3	58.15	38.96	0.54	1.01	0.83	0.51	0	44,200	75,200	33.0	29.0	1.26
B-46-6	57.52	38.24	0.44	1.00	0.88	0.50	1.42	40,100	68,600	22.0	11.0	1.26

* By difference.

* By difference.

Table 4

EFFECT OF LEAD ON MECHANICAL PROPERTIES OF TIN-FREE AND 0.3 PER CENT TIN MANGANESE BRONZE

Heat No.	Composition, Per Cent							Mechanical Properties				
	Cu	Zn*	Sn	Fe	Al	Mn	Pb	Yield Strength, lb. per sq. in. (0.1 per cent of gage length)	Tensile Strength, lb. per sq. in.	Elongation, Per Cent in 2-in.	Impact Strength at 21°C., Izod ft.-lb.	Cu-Zn Ratio
Effect of Lead on 0.3 Per Cent Sn Manganese Bronze												
B-30-3	59.68	37.76	0.29	0.88	0.92	0.47	0	34,500	68,200	46.0	46.0	1.33
B-46-2	58.27	39.08	0.29	1.01	0.84	0.51	0	39,100	72,700	38.0	41.5	1.27
B-30-4	59.28	37.44	0.29	0.88	0.91	0.47	0.73	34,000	65,700	29.0	16.0	1.33
B-46-5	57.60	38.37	0.23	1.00	0.89	0.50	1.41	36,900	66,900	23.5	11.5	1.27
Effect of Lead on Tin-Free Manganese Bronze												
192	58.57	39.15	0	1.12	0.85	0.31	0	31,200	68,800	48.4	43.0	1.29
B-30-1	59.56	38.15	0	0.88	0.93	0.48	0	35,500	64,200	46.5	53.0	1.33
B-32-1	57.83	39.73	0	0.96	0.96	0.52	0	34,600	72,700	38.0	41.5	1.24
B-46-1	58.53	39.23	0	0.84	0.90	0.50	0	32,600	69,200	44.0	50.5	1.28
B-32-2	57.91	39.56	0	0.98	0.97	0.53	0.05	35,200	72,400	37.0	40.0	1.24
B-32-3	58.07	39.40	0	0.97	0.97	0.54	0.05	34,900	71,900	35.5	37.5	1.25
B-32-4	58.21	39.24	0	0.99	0.93	0.51	0.12	34,200	71,100	37.0	32.0	1.27
B-32-5	58.16	39.00	0	0.99	0.95	0.51	0.39	33,700	68,900	30.3	23.5	1.27
B-30-2	59.42	37.80	0	0.88	0.93	0.48	0.49	34,300	62,400	31.0	20.5	1.34
B-47-2	58.34	38.28	0	1.07	0.91	0.46	0.94	30,600	63,400	29.0	15.0	1.30
B-47-3	58.14	38.13	0	1.00	0.91	0.46	1.36	31,100	62,400	26.5	13.5	1.30
B-47-4	57.92	37.88	0	1.08	0.89	0.45	1.78	30,800	62,900	28.5	12.5	1.30
B-46-4	57.68	38.59	0	0.98	0.87	0.50	1.38	32,000	60,100	18.5 B.O.	11.5	1.28
B-47-1	58.53	38.53	0	1.04	0.88	0.51	0.51	30,500	64,200	30.0	19.0	1.30

* By difference.

(phosphor-copper), arsenic, lead and antimony were added to 40-lb. heats of the representative composition (59.0 Cu, 38.5 Zn, 1.0 Fe, 1.0 Al, 0.5 Mn). All heats were deoxidized with phosphor-copper except those heats which were for comparison to manganese bronzes low in phosphorus. Each 40-lb. heat was cast into a "separately cast bar" mold which provided enough metal for ten 0.313-in. diameter tensile specimens, two standard Izod specimens, one sea water corrosion plate and a sample for chemical analysis. The pouring temperature was approximately 980°C. for all castings. Other techniques in the foundry were the same as those described in the previous section on experimental procedure.

19. The tensile strength of the 0.313-in. diameter tensile specimens exposed to 3N hydrochloric acid at 50°C. was taken as the accelerated test for dezincification. The threads of the test specimens were coated with apiezon wax to protect them from corrosion. The wax was closely adherent and did not flow or soften at 50°C. After the test, it was removed by melting in a bunsen flame. The specimens were suspended vertically in the solution in an 800 ml. beaker and an air line was introduced in the center. A separate beaker was used for each composition in order to avoid the possibility of contamination by soluble constituents. The beakers were held at the constant temperature of 50°C. controlled to ± 1°C. At one week intervals the solutions were renewed and two test specimens taken from each beaker for tensile testing (Fig. 6). The elongation could not be measured because the outer dezincified layer pulled apart from the undezincified core.

20. *Macroscopic Inspection.* After the tensile specimens had been broken, the 0.313-in. diameter cross sections were cut transversely, mounted in Wood's metal, polished, and etched lightly with a solution of ferric chloride. The light etching increased the contrast between the deep red layer of redeposited copper and the brassy unattacked interior. Due to the brittleness of the redeposited copper, transverse cracks opened in the outside layer during the tensile test as the unattacked metal inside elongated. This left the dimensions of the surrounding shell of copper but little changed and the cross sections shown in Fig. 7 permitted an estimate of the amount and nature of the dezincification.

Discussion

21. *The Tensile Tests.* The results of the tensile test shown in

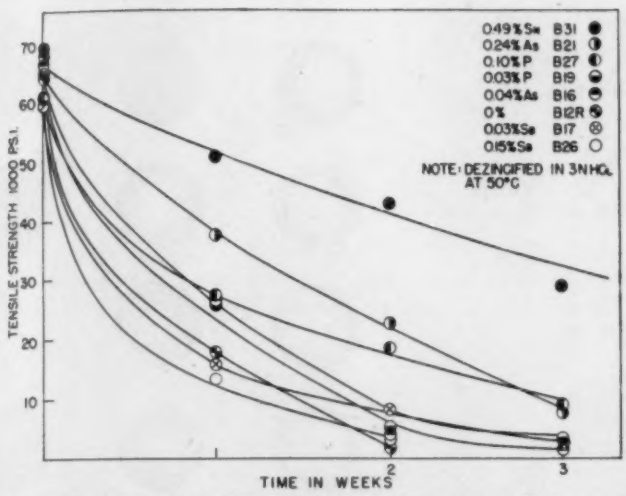

FIG. 6—EFFECT OF ADDITIONS ON TENSILE STRENGTH OF DEZINCIFIED MANGANESE BRONZE.

Fig. 6 indicate that the 3N hydrochloric acid solution is a severe test and will cause lower mechanical properties in manganese bronze even when it contains nearly 0.50 per cent of tin. It appears from the short time tests that tin is the only element to give appreciable resistance to dezincification. This comparison which, because of ductility requirements, is based of necessity on unequal percentages shows that the arsenic and phosphorus, 0.05 to 0.3 per cent, are much less beneficial than tin. Lead is still less beneficial, as shown in Table 5, and antimony is probably harmful in amounts from 0.05 to 0.1 per cent. Although it has been assumed that the order of resistance to dezincification is the same in the accelerated test as it would be in service, no final conclusions can be drawn until suitable long time investigations have been carried out to calibrate the short time tests.

22. *Macroscopic Examination.* The macrographs in Fig. 7 show qualitatively the same trend as the curves of decreasing tensile strength in Fig. 6. If the samples had not been uniformly dezincified in every instance, it would not be so easy to correlate the macroscopic details with the damage done. The somewhat beneficial effects of arsenic and phosphorus are shown in the 3-week specimens of B26 and B27 (Fig. 7), and the harmfulness of antimony is apparent in the 2-week specimen of B26 which is completely dezincified in the beta matrix. In fact, the one-week specimen with 0.1 per cent antimony (B26) was attacked more than the

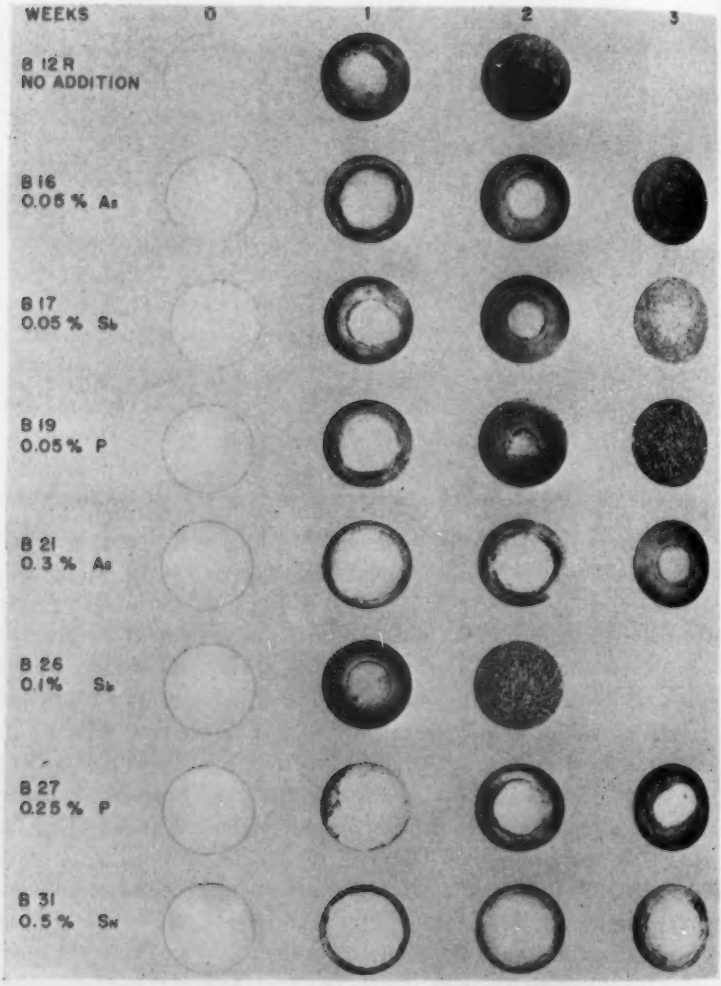

FIG. 7—EFFECT OF ADDITIONS ON THE DEZINCIFICATION OF MANGANESE BRONZE.

sample with no additions, B12R (Fig. 7). It will be noted that the residual alpha in B26 was unattacked, still retaining its dendritic form. This agrees with previous work in which antimony was classified as an inhibitor of dezincification in alpha brasses. The same may be true of the phosphorus in B19 which, although totally dezincified in the beta matrix, still shows islands of alpha apparently unattacked.

Table 5

EFFECT OF DEZINCIFICATION ON CAST MANGANESE BRONZE

Heat No.	Composition, Per Cent							Mechanical Properties						
								Yield Strength, lb. per sq. in. (0.1 per cent of gage length)	Elongation, Per Cent in 1.25-in.	Impact Strength at 21°C., Izod ft.-lb.	Tensile Strength, lb. per sq. in.			
	Cu	Zn*	Sn	Fe	Al	Mn	Other				Original	1 Week**	2 Weeks**	3 Weeks**
Tin-Free Manganese Bronze														
B12 R	58.44	39.36		0.95	0.83	0.42		30,200	46.4	54.0	68,500	18,100	1,950	0
B16	58.90	38.96		0.91	0.81	0.39	0.03As	31,500	42.0	37.0	66,400	27,700	3,100	1,690
B17	58.50	39.75		0.53	0.76	0.42	0.04Sb	31,100	23.6	16.0	60,000	15,900	8,100	3,000
B19	58.49	39.26		0.82	0.75	0.42	0.026P	29,700	38.0	32.0	65,100	26,000	5,400	3,800
B21	58.66	38.88		0.98	0.86	0.38	0.24As	28,300	33.0	20.0	64,100	37,800	22,900	7,900
B26	58.31	39.87		0.48	0.77	0.42	0.15Sb	31,100	19.2	10.0	61,300	12,600	3,500	0
B27	59.04	38.86		0.67	0.93	0.40	0.10P	28,800	36.0	41.0	61,000	26,500	18,000	8,950
Tin-Containing Manganese Bronze														
B31	58.60	38.59	0.49	1.04	0.85	0.43		39,000	30.0	25.0	69,700	50,900	43,000	29,000
Tin-Lead Manganese Bronze														
B30-1	59.56	38.15		0.88	0.93	0.48		35,500	46.5	53.0	64,200	27,000	5,100	0
B30-2	59.42	37.80		0.88	0.93	0.48	0.49Pb	34,000	31.0	20.5	62,300	38,900	15,300	100
B30-3	59.68	37.76	0.29	0.88	0.92	0.47		34,500	46.0	46.0	68,200	52,000	44,000	30,900
B30-4	59.28	37.44	0.29	0.88	0.91	0.47	0.73Pb	34,000	29.0	16.0	65,700	49,800	46,400	38,400
B30-5	59.34	37.96	0.51	0.86	0.87	0.46		39,000	41.5	35.5	69,900	54,000	49,400	47,400
B30-6	59.19	37.77	0.50	0.86	0.89	0.46	0.33Pb	39,500	33.5	22.0	68,900	58,200	46,700	38,000

* By difference.
** in 3 N HCL at 50°C.

SUMMARY AND CONCLUSIONS

23. When manganese bronze containing one per cent lead is thoroughly mixed before pouring, no serious segregation of the lead will occur during solidification. Since no other data are available, this statement applies only to cylinders 8-in. in diameter by 5-in. long, or smaller.

24. The limit to which lead can be added to manganese bronze, assuming that aluminum, iron and manganese are constant, depends upon the copper-zinc ratio and the tin content. Manganese bronze containing 1.5 per cent lead will meet mechanical requirements if the chemical composition and copper-zinc ratio are carefully controlled.

25. Tin is an effective inhibitor of dezincification; arsenic and phosphorus show beneficial effects, while antimony tends to be harmful.

ACKNOWLEDGMENTS

26. The authors wish to express their appreciation to Mr. Alden G. Church and Mr. William H. Baer for help afforded in the foundry, and to Mr. L. Singer for chemical analyses.

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(For discussion, see page 862)

The Effect of Lead on Some Mechanical Properties of Manganese Bronze

By George P. Halliwell*, Chicago, Ill.

Introduction

1. The unprecedented demand for non-ferrous metals and alloys for the war program has created scarcities that would hardly seem probable in a country as rich in these metals as is the United States. Such a condition has necessitated a reconsideration of the properties of our alloys and their ultimate use, resulting in a general system of down grading. In a recent article on this subject, Carter S. Cole[1] very clearly shows how this applies to copper-base alloys. By its application, primary metals and high grade mill scrap are retained for absolutely essential uses, and other copper-base alloys, for which secondary metals are or appear to be more plentiful, are substituted in other applications. Such an alloy is manganese bronze.

2. In the manufacture of high strength manganese bronze only virgin materials or their equivalent can be used if the combination of high tensile strength (100,000 to 120,000 lb. per sq. in.) and high ductility (12 to 20 per cent) is to be obtained. The situation with the regular grade manganese bronze containing 0.3 per cent max. lead (QQB726-b Grade A) at the present time is only slightly less restricted, as the supply of low-leaded secondary metal is insufficient to meet the tremendous tonnage of manganese bronze of this grade required by the war program. As this changes with the progress of the war and the availability of materials, it is impossible to accurately predict the changing scrap situation. Material that is scarce today may be available in large quantities in the near future, and vice versa. Therefore, some of the statements on availability of raw materials are based on the situation as it exists at the time this paper is written.

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* Director of Research, H. Kramer & Co.

[1] Superior numbers refer to references at end of paper.

Note: This paper was presented at a Brass and Bronze Founding Session of the 47th Annual Meeting, American Foundrymen's Association, St. Louis, Mo., April 29, 1943.

Note: This paper is sponsored by the Research Committee, Brass and Bronze Division, and is the result of work conducted by that Committee.

3. There are many castings being made today of manganese bronze containing 0.3 per cent max. lead which could be made from an alloy of higher lead content without changing, or, at most, only slightly changing the mechanical property requirements. An increase in the lead content of Federal Specification QQB726-b Grade A or SAE63 would release considerably more material for making such an alloy. The natural question would then be as to the effect upon the mechanical properties.

4. It has been generally accepted, in a qualitative measure, that lead lowers the tensile strength and, more particularly, the elongation of manganese bronze. The absolute quantitative effect when associated with various copper contents is not generally known. It is the purpose of this paper to place on record such quantitative data as have been obtained on a series of alloys.

TEST BARS

5. In the collection and correlation of any set of properties it is necessary that the equipment used be of a recognized standard, and that the condition of the material under test be such as would obtain under practical conditions. When properties are to be obtained on cast metal, the first requirement is a suitable test bar. While there is as yet no ideal universal test bar, there are some for each alloy which at least meet many of the requirements. A number of types have been tried, some of which have been discarded as unsuitable, for manganese bronze. Those found to be most satisfactory for this alloy are shown in Figs. 1 to 3. However, each of these have their individual disadvantages, which will be noted later.

6. The $\frac{5}{8}$-in. Web-Webbert bar, poured flat, gave the highest average tensile strength and elongation in most of the alloys. This bar is relatively easy to cut off and to machine. It has the disadvantage of sometimes breaking near or outside the gauge marks on those alloys having higher tensile strengths and lower elongations. Such breaks resulted in decreased ductilities and the results were voided for correlation of data.

7. The medium-V bar (Fig. 2) is cast in an open sand mold and represents a type used for some time by the author's company. They are cast two in a mold from a common pouring basin. This bar provides ample metal to feed that portion from which the test bar is cut, as shown in Fig. 2. The resulting properties are more uniform but a trifle lower than those obtained on the Web-Webbert

bar. It is easy to mold, but its chief disadvantage is the time required to cut off and machine the bars.

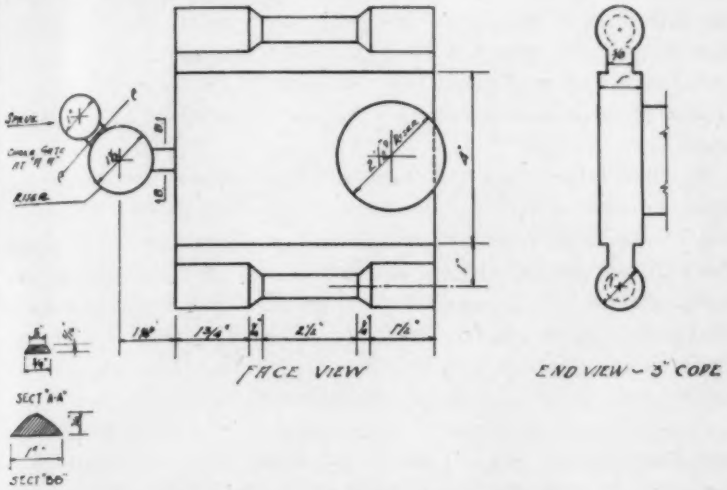

FIG. 1—FIVE-EIGHTHS IN. WEB-WEBBERT TEST BAR.

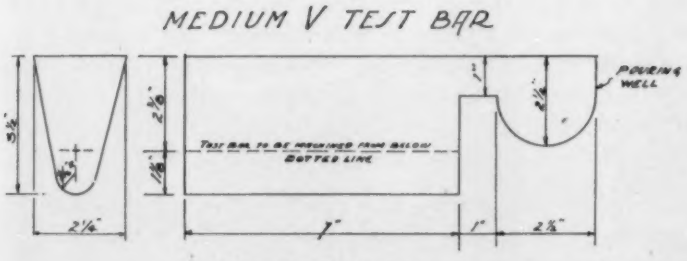

FIG. 2—MEDIUM-V TEST BAR.

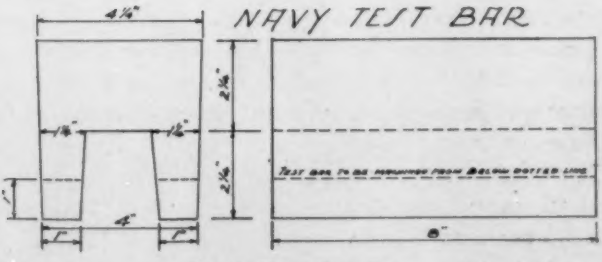

FIG. 3—NAVY TEST BAR.

8. The conventional Navy Bar is shown in Fig. 3. As in the case of the medium-V bar, it provides ample feeding of the liquid metal to the test bar section, but requires an undue amount of metal and is costly to machine. Its properties are equal to or slightly lower than the medium-V bar. Because of its large size, the bar is also unsuitable as a criterion for the properties in a casting of small cross section.

9. Two other types of bars were tried, wherein an attempt was made to cast the bars approximately to size and shape. These were the Crown Web-Webbert[2], which uses a green sand core around the reduced test bar section, and a bar used by Lunkenheimer[3] for some of their alloys, especially red metals. This latter bar is cast to size and shape and fed from large risers at each end of the bar. Both these bars developed internal shrinks in the reduced bar section, and were accordingly discarded as unsuitable for general use on manganese bronze. Apparently the cooling rate of the metal around the test section was too rapid to permit proper feeding, even from such a liberal head of metal. This difficulty might have been corrected with a higher pouring temperature, which was considered unnecessary for this alloy.

10. In addition, tests were made on bars cast in a baked sand core. This was essentially a Web-Webbert bar fed along its entire length from a liberal riser. The properties of this bar were lower than those of the first three types mentioned.

METAL USED

11. In order to obtain a true picture of the effect of lead on the properties of manganese bronze, a series of alloys was made from virgin metals. Two heats (700 lb.) were initially made containing 54 per cent and 64 per cent copper respectively, and cast into ingots of approximately 20-lb. weight. From these heats smaller ones of varying copper content were compounded. Except in the heats having maximum and minimum copper contents, gates, sprues and castings were returned to succeeding heats for remelting, without any apparent detrimental effect upon the mechanical properties.

12. In a similar manner, heats were made from secondary metal and the lead content adjusted to approximately 0.15 per cent, 0.35 per cent, 0.6 per cent, 1 per cent and 1.5 per cent. All heats made from secondary metal contained about 0.1 per cent tin. Through-

out all the alloys the essential elements, other than those already mentioned, were held within the following limits:

	Per Cent
Aluminum	1.05 ± 0.05
Iron	1.30 ± 0.20
Manganese	0.45 ± 0.15
Zinc	Balance

Of these elements aluminum is the most important, since it has the greatest effect on the structural and mechanical properties for any copper content within the range covered by the specifications.

MELTING AND POURING

13. Experimental heats of 125 to 175 lb. were made in a no. 60 clay graphite crucible in an oil-fired furnace. All melts were heated to 1825 to 1850°F. before pulling, cooled to 1725 to 1775°F. before leaving the pyrometer station, and poured. A portable potentiometer in conjunction with a protected tip thermocouple was used to measure temperatures. Test bars were allowed to cool in the sand for one hour before dumping, at which time they were at black heat.

Testing

14. All bars were threaded and machined according to A.S.T.M. specifications[1] and tested in a 60,000-lb. hydraulic type tensile machine. The 0.5 per cent yield strength under load was obtained with a dial extensometer reading to 0.00005-in.

RESULTS AND DISCUSSION

15. The data on mechanical properties for each series of alloys are given in Tables 1 to 6. Because of lack of space, individual tests are not shown. The results given in the tensile strength, yield strength and elongation columns are the average of the number of tests indicated in the test column. A weighted average of the Web-Webbert, medium-V and Navy Bars is also shown. The average properties of the Web-Webbert bars are shown graphically in Figs. 4 to 7.

16. The tensile strength of each series of alloys shows a decrease with an increase in copper, slowly at first, and more rapidly be-

Table 1

LEAD—LESS THAN 0.005 PER CENT. SERIES 1

Heat No.	Composition, Per Cent						Type Bar	No. of Tests	Tensile Strength, lb. per sq. in.	0.5 Per Cent Yield Strength, lb. per sq. in.	Elongation, per cent in 2-in.	Brinell Hardness, 3000 kg. Load
	Cu	Sn	Pb	Fe	Mn	Al						
568	56.01		0.004	1.19	0.41	1.08	5/8WW*	6	82500	27600	28.4	148
							MV**	2	81200	27550	26.6	
							NB‡	2	82100	27500	27.8	
							Average	10	82160	27570	27.9	
585	57.10	Tr.	0.004	1.07	0.43	1.08	5/8WW	4	81870	29800	27.5	152
							MV	3	80400	30250	24.0	
							NB	2	79650	30050	22.5	
							Average	9	80880	30000	25.2	
583	57.13	Tr.	0.004	1.06	0.41	1.09	5/8WW	4	80800	29700	26.1	152
							MV	3	79200	29600	24.0	
							NB	2	80000	30100	23.8	
							Average	9	80090	29700	24.8	
681	57.16	Tr.	0.003	1.07	0.38	1.08	5/8WW	4	79860	29800	25.6	145
							MV	3	77800	30000	23.2	
							Average	7	78970	29900	24.6	
587	58.33	Tr.	0.003	1.08	0.39	1.09	5/8WW	5	76000	27550	33.7	135
							MV	2	73900	27100	30.8	
							NB	2	74000	26950	30.0	
							Average	9	75090	27310	32.1	

							Average	9	75090	27310	32.1	
680	58.93	Tr.	0.004	1.12	0.41	1.10	5/8WW	3	72700	26000	40.4	134
							MV	3	72200	25800	39.2	
							Average	6	72300	25900	39.8	
658	59.65	Tr.	0.004	1.12	0.39	1.06	5/8WW	5	69900	24750	44.2	121
							MV	3	69400	24650	43.0	
							Average	8	69710	24700	43.7	
586	60.31	Tr.	0.004	1.12			5/8WW	5	67770	23450	47.4	116
							MV	3	67650	23600	46.5	
							NB	2	66950	23550	45.7	
							Average	10	67570	23510	46.8	
569	61.55	Tr.	0.004	1.15	0.45	1.10	5/8WW	6	63660	22600	52.9	112
							MV	2	64650	23550	51.25	
							NB	2	63150	22350	54.0	
							Average	10	63750	22740	52.8	
563	63.57			1.15	0.45	1.10	5/8WW	4	59100	20500	55.5	91
							NB	2	58100	20000	60.4	
							MV	2	59600	21900	58.3	
							Average	8	58970	20720	57.4	
539	63.90			1.15	0.45	1.10	5/8WW	2	59000	20500	57.75	96
							NB	2	58000	20100	58.0	
							MV	4	58800	20600	55.3	
							Average	8	58650	20450	56.6	

* Five-eighths in. Web-Webbert test bar.
** Medium-V test bar.
‡ Navy Bar.

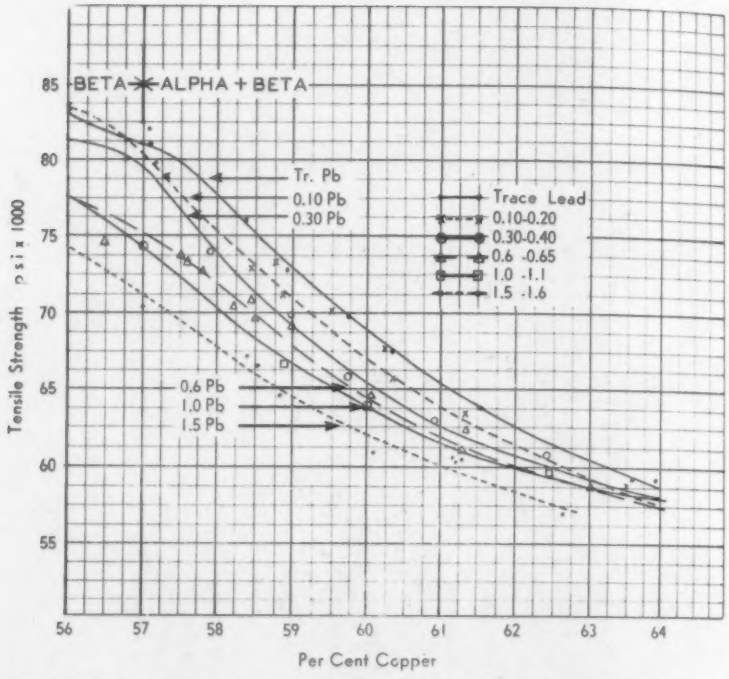

FIG. 4—EFFECT OF LEAD ON TENSILE STRENGTH OF MANGANESE BRONZE (60,000 TO 80,000 LB. PER SQ. IN. TENSILE STRENGTH) OF VARYING COPPER CONTENT.

yond 57 per cent copper. This is as would be expected since most of these alloys consist of two constituents; the hard, strong beta phase of relatively low ductility and the soft, low tensile alpha phase of high ductility. It will be readily recognized that the resulting properties of such alloys are dependent upon the amounts of these two phases present. Since iron, manganese, aluminum and tin are kept reasonably constant, the only other variables in composition are lead, copper, and zinc. Any change in the copper content means a corresponding change of the zinc in the opposite direction, since they vary reciprocally at each other's expense. Any change in properties for a particular copper content then will be due to the presence of lead, the conditions of melting, pouring and molding being approximately the same.

Tensile Strength

17. Lead up to 0.35 per cent seems to have very little effect upon the tensile strength of the all beta alloys at approximately

56 per cent copper. The slight superiority of the alloys containing 0.15 per cent lead (Table 2) at 56 per cent copper may be due to the small amount of tin (0.1 per cent) present, or it may also be due to an insufficient amount of data to warrant a true average. As this paper was not intended to be a statistical study, several such irregularities will be noted.

18. A distinct change in properties of the alloys containing up to 0.35 per cent lead occurs at 57 per cent copper. This is due to the initial appearance of the alpha constituent. There is also a decrease in tensile strength for any one copper content as the lead increases, which amounts to about 2000 lb. per sq. in. for the 0.15 per cent lead (Table 2) and about 3500 lb. per sq. in. for the 0.35 per cent lead (Table 3).

19. In the higher leaded alloys (Tables 4, 5 and 6) the decrease

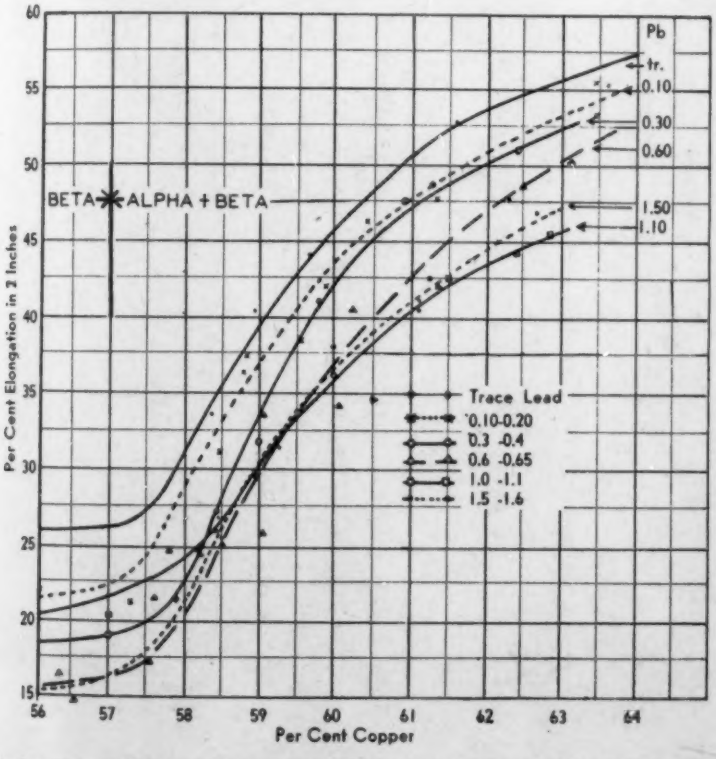

FIG. 5—EFFECT OF LEAD ON ELONGATION OF MANGANESE BRONZE (60,000 TO 80,000 LB. PER SQ. IN. TENSILE STRENGTH) OF VARYING COPPER CONTENT.

Table 2

LEAD 0.10 TO 0.20 PER CENT. SERIES 2

Heat No.	Composition, Per Cent						Type Bar	No. of Tests	Tensile Strength, lb. per sq. in.	0.5 Per Cent Yield Strength, lb. per sq. in.	Elongation, per cent in 2-in.	Brinell Hardness, 3000 kg. Load
	Cu	Sn	Pb	Fe	Mn	Al						
571	56.01	0.12	0.21	1.38	0.46	1.09	5/8WW	4	83000	32700	21.9	157
							MV	2	81100	31900	19.4	
							NB	2	78900	31800	17.25	
							Average	8	81500	32250	20.1	
574	57.28	0.12	0.14	1.41	0.46	1.08	5/8WW	5	78800	32500	21.3	150
							MV	4	77500	32420	19.1	
							NB	2	76950	31550	21.0	
							Average	11	78000	32300	20.4	
584	57.28	0.12	0.18	1.31	0.48	1.04	5/8WW	5	78700	32200	24.2	150
							MV	3	78480	31960	22.9	
							NB	2	77250	32250	20.5	
							Average	10	78340	32140	23.1	
578	58.47	0.12	0.17	1.36	0.51	1.05	5/8WW	6	73130	28270	31.2	130
							MV	6	72200	27700	30.0	
							Average	12	72660	27980	30.6	
660	58.76		0.15				5/8WW	8	73230	27120	36.5	126
677	58.74						MV	6	72600	26900	36.3	
							Average	14	72960	27020	36.4	

580	58.81	0.11	0.16	1.29	0.51	1.05	5/8WW	5	71580	27050	37.6	125	
							MV	4	70600	26300	36.4		
							NB	2	69850	26000	37.7		
							Average	11	70900	26590	37.2		
659	59.51		0.15	1.13	0.30	0.97	5/8WW	2	70300	25750	38.5	124	
							MV	3	69400	25000	36.5		
							Average	5	69760	25500	37.3		
581	59.65	0.12	0.18	1.32	0.42	1.03	5/8WW	5	68140	25340	42.1	117	
							MV	4	68100	25310	43.3		
							NB	2	67600	25000	42.7		
							Average	11	68020	25260	42.6		
582	60.41	0.11	0.14	1.38	0.51	1.05	5/8WW	4	65500	23960	46.4	111	
							MV	3	65900	24460	44.6		
							NB	2	65350	24100	42.5		
							Average	9	65600	24150	44.9		
570	61.30	0.11	0.13	1.46	0.60	1.06	5/8WW	6	63300	22850	47.9	105	
							MV	2	63250	22900	49.9		
							NB	2	62250	22500	50.1		
							Average	10	63080	22790	48.7		
567	63.46	0.10	0.11	1.34	0.64	1.08	5/8WW	6	58500	20100	55.7	96	
							MV	2	58700	20200	56.5		
							NB	2	58000	19860	54.5		
							Average	10	58440	20070	55.6		
661	63.45		0.17				5/8WW	3	58000	20300	53.5	94	
							MV	3	57300	20000	52.5		
							Average	6	57650	20150	53.0		

Table 3

LEAD 0.30 TO 0.40 PER CENT. SERIES 3

Heat No.	Composition, Per Cent						Type Bar	No. of Tests	Tensile Strength, lb. per sq. in.	0.5 Per Cent Yield Strength, lb. per sq. in.	Elongation, per cent in 2-in.	Brinell Hardness, 3000 kg. Load
	Cu	Sn	Pb	Fe	Mn	Al						
590	55.88	0.13	0.30	1.31	0.40	1.08	5/8WW	5	82300	31790	20.7	157
							MV	4	82600	31800	19.0	
							NB	2	82375	31800	19.0	
							Average	11	82400	31800	19.8	
593	56.99	0.12	0.37	1.32	0.35	1.06	5/8WW	5	79680	33400	18.5	156
							MV	4	78300	32200	18.4	
							NB	2	78400	33000	19.2	
							Average	11	78830	32840	18.6	
597	57.90	0.11	0.35	1.11	0.38	1.07	5/8WW	5	74000	29970	21.6	138
							MV	4	73900	29500	23.4	
							Average	9	73990	29730	22.4	
591	58.45	0.11	0.37	1.35	0.46	1.08	5/8WW	5	71600	28300	27.5	131
							MV	3	71800	28500	25.5	
							NB	2	70500	27900	22.0	
							Average	10	71440	28280	25.8	
598	58.98	0.11	0.36	1.34	0.48	1.09	5/8WW	6	69960	26490	31.9	124
							MV	4	69520	26300	30.8	
							Average	10	69780	26410	31.5	

589	59.75	0.13	0.38	1.27	0.50	1.04	5/8WW	5	65800	24200	41.1	113	
							MV	3	66100	24500	38.4		
							NB	2	65500	24350	38.8		
							Average	10	65830	24320	39.8		
592	61.26	0.12	0.41	1.31	0.54	1.06	5/8WW	4	62850	22700	47.6	108	
							MV	3	62650	22850	44.7		
							NB	2	62100	22300	47.5		
							Average	9	62610	22660	46.6		
594	62.40	0.11	0.31	1.35	0.45	1.06	5/8WW	4	60500	21410	51.0	99	
							MV	3	60250	21280	49.0		
							NB	2	60150	22350	48.5		
							Average	9	60300	21500	49.8		

Table 4

LEAD 0.60 PER CENT. SERIES 4

Heat No.	Composition, Per Cent						Type Bar	No. of Tests	Tensile Strength, lb. per sq. in.	0.5 Per Cent Yield Strength, lb. per sq. in.	Elongation, per cent in 2-in.	Brinell Hardness, 3000 kg. Load
	Cu	Sn	Pb	Fe	Mn	Al						
601	56.34	0.10	0.63	1.33	0.49	1.01	5/8WW	6	77400	32730	16.6	155
							MV	3	76460	31960	16.2	
							Average	9	77100	32350	16.4	
602	56.52	0.12	0.61	1.27	0.49	.99	5/8WW	5	74720	32720	15.3	153
							MV	3	75950	32450	16.6	
							Average	8	75180	32610	15.8	
609	57.52	0.10	0.63	1.35	0.50	1.03	5/8WW	6	73800	30950	17.3	146
							MV	4	73600	31200	16.9	
							Average	10	73700	31040	17.2	
605	57.57	0.10	0.61	1.35	0.48	1.03	5/8WW	5	73350	29680	21.7	138
							MV	3	73350	29830	21.7	
							Average	8	73350	29730	21.7	
683	57.80	0.10	0.63	1.04	0.40	1.01	5/8WW	5	72790	28350	24.8	134
							MV	5	71180	28670	20.6	
							Average	10	71980	28510	22.7	
685	58.16		0.64	1.04		1.03	5/8WW	3	71310	27800	24.5	130
							MV	3	71050	26800	26.3	
							Average	6	71180	27300	25.4	
687	58.20	0.09	0.69	1.09		1.01	5/8WW	6	70380	26900	24.7	128
							MV	3	69750	27300	25.2	
							Average	9	70170	27030	25.0	

							MV	3	69750	27300	25.2	
							Average	9	70170	27030	25.0	
614	58.44	0.10	0.62	1.34	0.50	1.05	5/8WW	5	70850	27530	26.1	130
							MV	3	70000	27600	25.7	
							Average	8	70550	27620	26.4	
607	58.49	0.10	0.60	1.33		1.03	5/8WW	5	69550	27430	25.3	125
							MV	3	69600	27360	26.4	
							Average	8	69570	27400	26.9	
622	59.04	0.10	0.51				5/8WW	4	69400	26300	33.8	124
							MV	4	68975	26200	31.5	
							Average	8	69180	26200	32.7	
610	59.18	0.10	0.61	1.36		1.06	5/8WW	5	66000	25400	25.7	120
							MV	3	67400	25400	27.5	
							Average	8	66500	25400	26.4	
611	60.05	0.10	0.63	1.31		1.06	5/8WW	5	64600	24230	34.4	112
							MV	4	64600	24190	35.0	
							Average	9	64600	24210	34.7	
688	60.20	0.10	0.69	1.09		1.01	5/8WW	5	64540	23710	40.6	111
							MV	3	64000	23675	36.6	
							Average	8	64340	23700	39.1	
612	61.30	0.10	0.67	1.30		1.07	5/8WW	5	62500	22800	42.1	105
							MV	3	62000	22350	42.05	
							Average	8	62370	22660	42.1	
606	62.51	0.10	0.57	1.49		1.05	5/8WW	5	59100	21040	48.8	96
							MV	3	59100	20900	46.9	
							Average	8	59100	20980	48.1	
684	63.08		0.65	1.14		1.02	5/8WW	5	57350	19900	50.7	93
							MV	3	57300	19500	51.3	
							Average	8	57330	19750	50.9	

Table 5

LEAD 1 PER CENT. SERIES 5

Heat No.	Composition, Per Cent						Type Bar	No. of Tests	Tensile Strength, lb. per sq. in.	0.5 Per Cent Yield Strength, lb. per sq. in.	Elongation, per cent in 2-in.	Brinell Hardness, 3000 kg. Load
	Cu	Sn	Pb	Fe	Mn	Al						
615	57.00	0.10	1.01	1.22		1.01	5/8WW	6	74400	30700	20.5	142
							MV	3	73400	30100	20.8	
							Average	9	74050	30500	20.6	
613	58.17	0.10	1.07	1.30		1.07	5/8WW	4	69800	27640	24.7	128
							MV	3	68900	27350	23.6	
							Average	7	69410	27660	24.2	
616	58.90	0.10	1.06	1.27		1.05	5/8WW	6	66700	25900	29.6	121
							MV	3	66100	25650	28.4	
							Average	9	66600	25800	29.2	
617	60.04	0.10	1.09	1.31		1.06	5/8WW	5	63900	23840	36.6	111
							MV	3	63350	23800	35.8	
							Average	8	63700	23800	36.3	
620	61.49	0.10	1.04	1.28		1.06	5/8WW	5	59900	21790	42.3	99
							MV	3	59500	21500	43.3	
							Average	8	59750	21700	42.7	
621	62.38	0.10	1.04	1.30		1.07	5/8WW	4	59750	21400	44.5	98
							MV	3	59800	21400	44.6	
							Average	7	59775	21400	44.5	
690	62.80		1.20	1.13		1.02	5/8WW	3	57960	20850	45.7	93
							MV	3	57760	20650	45.4	
							Average	6	57860	20750	45.6	

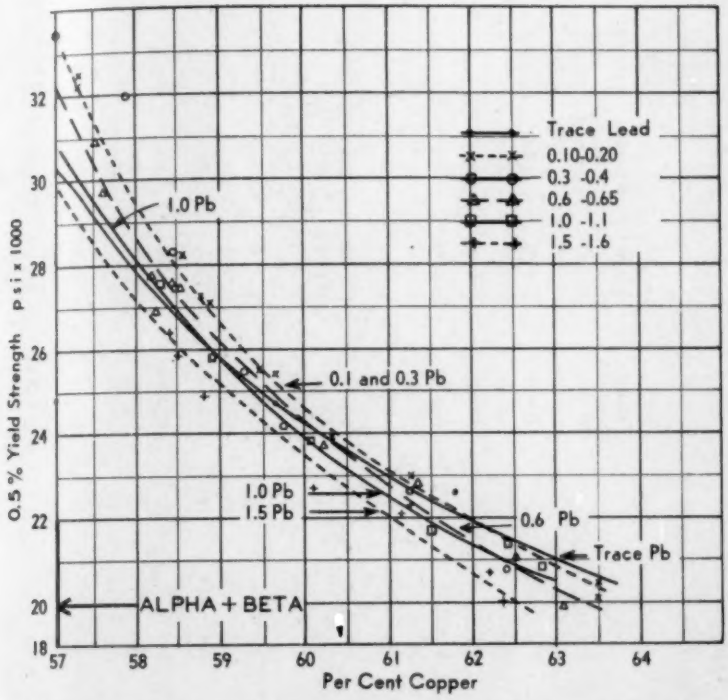

FIG. 6—EFFECT OF LEAD ON 0.5 PER CENT YIELD STRENGTH OF MANGANESE BRONZE (60,000 TO 80,000 LB. PER SQ. IN. TENSILE STRENGTH) OF VARYING COPPER CONTENT.

in tensile strength becomes more marked, reaching a maximum of 10,000 lb. per sq. in. for 1.5 per cent lead alloys at 57 per cent copper (Table 6). It will also be noted that the change in slope of the curves of these higher leaded alloys is less marked at 57 per cent copper, and that the decrease within the all beta region, 56 to 57 per cent copper, is quite marked, emphasizing the general effect of increased lead. The differences in tensile strength between the various alloys decrease steadily beyond 62 per cent copper, and approach a minimum corresponding to an all alpha alloy.

Ductility

20. The effect of lead on ductility is shown in Fig. 5. The data are more erratic than those on tensile strength, which would seem to indicate that this property is more sensitive to the presence of lead. Considerable license has been taken in drawing some of the

Table 6

LEAD 1.5 PER CENT. SERIES 6

Heat No.	Composition, Per Cent						Type Bar	No. of Tests	Tensile Strength, lb. per sq. in.	0.5 Per Cent Yield Strength, lb. per sq. in.	Elongation, per cent in 2-in.	Brinell Hardness, 3000 kg. Load
	Cu	Sn	Pb	Fe	Mn	Al						
625	57.02		1.50	1.22		1.01	5/8WW	5	70400	29810	16.6	140
							MV	3	71000	29400	19.2	
							Average	8	71000	29650	17.6	
691	58.40	0.10	1.57	1.22		1.02	5/8WW	4	67100	26500	27.1	124
							MV	3	66400	26850	23.8	
							Average	7	66800	26650	25.9	
626	58.50		1.70	1.23		1.01	5/8WW	4	66050	25900	25.3	120
							MV	3	65000	25600	26.0	
							Average	7	65600	25800	25.6	
632	58.82		1.52	1.20		1.02	5/8WW	3	64600	24850	29.0	119
							MV	3	64600	24700	30.0	
							Average	6	64600	24750	29.5	
692	59.23	0.10	1.50			1.10	5/8WW	4	65550	25480	31.4	117
							MV	3	64850	25400	29.6	
							Average	7	65250	25460	30.6	
627	60.10	0.10	1.59	1.26		1.06	5/8WW	3	61000	22700	38.0	104
							MV	3	60400	22750	36.0	
							Average	6	60700	22725	37.0	

693	61.15	0.10	1.54	1.28	1.10	5/8WW	4	60550	22100	41.3	96
						MV	3	59400	21600	41.4	
						Average	7	60060	21900	41.4	
633	61.16	0.10	1.58	1.28	1.05	5/8WW	3	60600	22300	40.3	101
						MV	3	60000	22000	39.0	
						Average	6	60300	22150	39.6	
679	61.24	0.10	1.59	1.28	1.05	5/8WW	3	60700	22300	42.6	100
						MV	3	60260	22000	39.9	
						Average	6	60380	22150	41.2	
694	62.18	0.10	1.44	1.17	1.02	5/8WW	4	57970	20750	47.7	93
						MV	3	57430	20750	46.2	
						Average	7	57720	20750	47.0	
634	62.66	0.10	1.58	1.22	1.05	5/8WW	3	56800	20080	47.0	91
						MV	3	56500	20100	46.0	
						Average	6	56650	20100	46.5	

curves to conform to these scattered points, resulting in some curves being "out of position," although the general trend is quite clear.

21. The effect of lead upon ductility is almost the reverse of that on tensile strength and, as with the tensile strength, an abrupt and greater change occurs in ductility at 57 per cent copper. Beginning at 57 per cent copper, the alloys containing 0.15 per cent lead show from 2 to 4 per cent less elongation with an increase in copper than do those made from the virgin mixture. All of the other series of alloys follow a similar pattern, with the same or slightly smaller proportionate decreases in ductility, except the 1.0 per cent series.

22. In the range 56 to 58 per cent copper, it lies between the 0.15 and 0.35 per cent series, but with further increase in copper it changes its relative position and ends up below all the others. There is no apparent reason for this.

23. All the series of alloys show a rapid increase in ductility, varying from 5 to 10 per cent for every per cent increase in copper within the range 57 to 60 per cent copper. This reaches a maximum at 58 to 59 per cent copper. In contrast to the tensile strength curves, the differences in ductility at any particular copper content increase with the copper content.

Yield Strength

24. The data on 0.5 per cent yield strength are shown in Fig. 6. These curves are of interest in that they have no counterpart in any of the other curves. Irrespective of lead content, each series approaches a limit between 56.5 and 57 per cent copper.

25. Beyond 58 per cent copper there is very little difference between the yield strength of any of the alloys, although the 1.5 per cent series consistently is about 1000 lb. per sq. in. lower than that of the virgin alloy. At the higher copper contents most of the curves cross that of the virgin alloy and show slightly lower yield strengths.

Hardness

26. Brinell hardness curves are shown in Fig. 7. Alloys containing up to 0.6 per cent lead show a higher hardness than does the virgin alloy at 56 to 57 per cent copper. At the latter copper content, the 1 per cent and 1.5 per cent leaded alloys lose their advantage, while the remaining alloys maintain their superior hard-

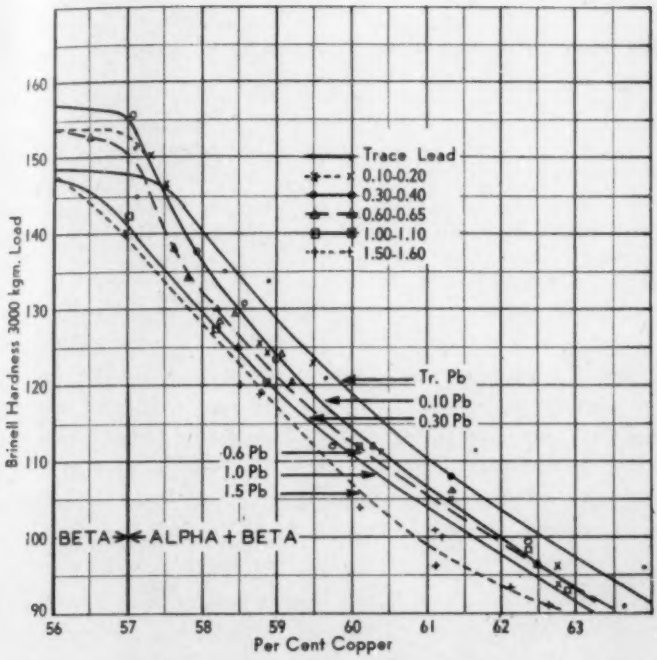

FIG. 7—EFFECT OF LEAD ON BRINELL HARDNESS OF MANGANESE BRONZE (60,000 TO 80,000 LB. PER SQ. IN. TENSILE STRENGTH) OF VARYING COPPER CONTENT.

ness up to 57 per cent copper. Beyond this point, the virgin mixture is a trifle harder—5 points—at any particular copper concentration than the alloy containing 0.15 per cent lead.

27. With the information shown in these curves, it is very easy to select limits of copper and lead that will enable foundrymen to meet the various specifications. To facilitate this, the previous data have been rearranged in Fig. 8 to show the tensile strength and elongation as a function of the lead content at a series of constant copper contents.

28. The two general specifications covering the 60,000 to 80,000 lb. per sq. in. tensile strength manganese bronze are as shown in Table 7.

Regular Manganese Bronze

29. The properties of regular manganese bronze can be readily met by any alloy within the range of copper specified provided the

Table 7

MECHANICAL PROPERTIES OF REGULAR AND LEADED MANGANESE BRONZE

	Manganese Bronze	
	Regular	*Leaded*
Copper, per cent	55–60	56–62
Lead, per cent	0– 0.4	0.5– 1.5
Tensile strength, min., lb. per sq. in.	65000	60000
Yield strength, lb. per sq. in.	25000	20000
Elongation, per cent in 2-in.	20 or 25	20

lead is kept below 0.15 per cent. If the lead is increased to 0.4 per cent, then the copper must likewise be increased. The maximum copper content permissible with 0.4 per cent lead and still maintaining 65,000 lb. per sq. in. tensile strength is between 60 and 61 per cent, while slightly more than 57 per cent is needed to maintain 20 per cent elongation. So, if 0.4 per cent lead is present, the range of copper content is limited to 57 to 60.5 per cent, rather than 55 to 60 per cent for the lower lead alloys.

30. If an elongation of 25 per cent is desired with 65,000 lb. per sq. in. tensile strength, the limits of composition are considerably more restricted. If the alloy has a copper content of between 55 and 57 per cent, then lead must be entirely absent in order to meet the ductility requirements. If the maximum permissible amount of lead (0.4 per cent) is used, then the copper must be raised to about 58.5 per cent. This means that copper may be varied only 1.5 per cent (58.5 to 60 per cent) if these properties are to be maintained with the maximum allowable lead present.

Leaded Manganese Bronze

31. In the case of leaded manganese bronze, a wider range of composition is available. A tensile strength of 60,000 lb. per sq. in. may be obtained at any lead content up to 1.5 per cent when in combination with any copper from 54 to 61 per cent. Some difficulty might be experienced in obtaining this strength with the maximum lead and copper contents. If copper higher than 61 per cent is used, then lead must be drastically reduced. This is shown in the last two curves, 61 and 62 per cent copper on tensile strength chart (Fig. 8).

32. The minimum copper content permitted in conjunction with maximum lead in order to obtain 20 per cent or more elongation

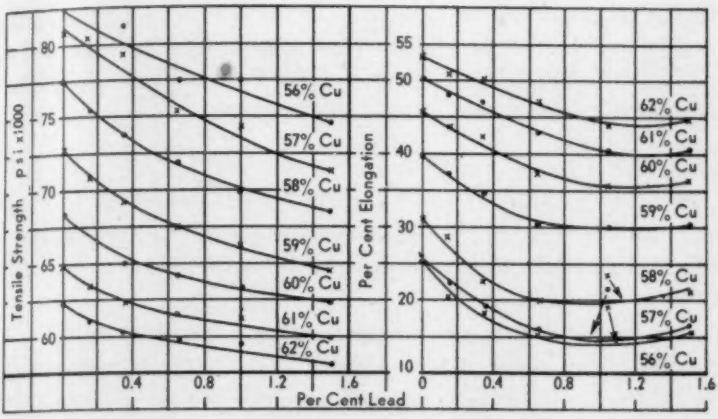

FIG. 8—EFFECT OF LEAD ON TENSILE STRENGTH AND ELONGATION OF MANGANESE BRONZE (60,000 TO 80,000 LB. PER SQ. IN. TENSILE STRENGTH) OF CONSTANT COPPER CONTENT.

is 58 per cent. Even at this copper content some difficulty may be experienced, but a slight increase in copper will materially increase the elongation.

33. It is to be noted (Fig. 8) that there is little change in elongation for any one copper content when lead is increased from 0.8 to 1.5 per cent. There is even a tendency toward a slight increase in ductility as the lead approaches the maximum value, especially at the lower copper contents. This is due to the fact that lead does not enter into solid solution, and hence has no zinc or copper "equivalence value." However, the copper-zinc ratio is increased, and as a result increases the amount of alpha present. The increased amount of lead would also tend to reduce the cast metal grain size, a factor which always is beneficial to ductility.

Iron and Aluminum Content

34. However, it is to be remembered that the limitations imposed on composition in connection with mechanical properties will be modified with a change in the iron and, especially, aluminum contents. A higher iron within the limitations of the specification will have little effect, and iron near the lower limit will have a minor effect. All properties will probably be reduced slightly for any particular copper content.

35. As previously mentioned in this paper, with the exception of silicon, aluminum has the greatest effect upon the mechanical

properties of manganese bronze. Any reduction in aluminum will produce a reduction in tensile strength, yield and hardness with an increase in elongation, while an increase in aluminum will produce the opposite effect on all mechanical properties.

Variation in Copper and Lead

36. The general effect of a variation in copper (57 to 62 per cent) and lead upon the mechanical properties of the manganese bronze described in this paper may be summarized as shown in Table 8.

37. In Table 8 it can be seen that one per cent lead has its greatest effect on the tensile strength at the lower copper contents. Its effect upon ductility is just the opposite to an equivalent amount of copper.

SUMMARY AND CONCLUSIONS

38. From the test results the following conclusions may be drawn:

1. A marked change in properties occurs at about 57 per cent copper. Such a change is caused by the precipitation of the alpha constituent and is inherent to the copper-zinc alloy. The exact copper content at which this change occurs is dependent upon the amount of the other metals present, especially aluminum.

Table 8

EFFECT OF COPPER AND LEAD VARIATIONS ON MECHANICAL PROPERTIES OF MANGANESE BRONZE

	Mechanical Properties			
Alloy	*Tensile Strength, lb. per sq. in.*	*0.5 per cent Yield Strength, lb. per sq. in.*	*Elongation, per cent in 2-in.*	*Brinell Hardness, 3000 kg. Load*
Average change in properties due to addition of one per cent copper				
Virgin Mixture	—3900	—1600	+5.6	— 8.8
Lead series, 0.15 per cent	—3900	—2400	+5.7	—11.0
Lead series, 1.50 per cent	—2600	—1600	+5.7	— 9.2
Average change in properties due to addition of one per cent lead				
Copper, 57 per cent ...	—6600	—2600	—6.0	— 5.0
Copper, 62 per cent ...	—2800	—1000	—6.0	— 6.5

Table 9

COPPER AND LEAD CONTENTS OF MANGANESE BRONZE

Tensile Strength, lb. per sq. in.	*Elongation, per cent in 2-in.*	*Copper, per cent*	*Lead, per cent*
65000	20	56.0–62.0	0.15
65000	20	57.0–60.5	0.40
65000	25	56.0–60.5	Trace
65000	25	58.5–60.0	0.40
60000	20	56.0–62.0	0 –0.2
60000	20	58.0–61.0	0.2 –0.6
60000	20	58.5–61.0	0.6 –1.5

2. In general, lead lowers all the mechanical properties of manganese bronze. The increase in yield strength of secondary metal over that of virgin metal is not satisfactorily explained as yet. Up to 0.6 per cent lead, the tensile strength is lowered 6000 lb. per sq. in., elongation about 10 per cent, Brinell hardness about 10 points, while yield strength shows an increase of about 1000 lb. per sq. in.

3. In order to meet the various specifications, the copper and lead contents must be as shown in Table 9.

4. The addition of lead beyond 0.6 per cent has a considerably smaller proportionate effect upon the ductility in the range of copper 56 to 60 per cent, but an unreduced effect upon the tensile strength.

ACKNOWLEDGMENTS

39. Acknowledgment is made at this time for the assistance of John Duffy in the molding and machining of test bars, to Elmer Field for mechanical testing, to A. B. Shapiro for the chemical analysis, and to H. Kramer & Co. for permission to publish these results.

References

1. Cole, Carter S., *"Down Grading Chart for Brasses and Bronzes,"* METALS AND ALLOYS, March, 1943, p. 406, and AMERICAN FOUNDRYMAN, Feb., 1943, p. 21.

2. Navy Specifications QQ-M-151-a, Fig. 10A, p. 21.

3. Bolton, J. W., *"Comparison of A.S.T.M. and Other Test Bars for Bronze Castings,"* Report to Committee B-5, Sub.-10, A.S.T.M. (1943).

4. A.S.T.M. Standards, Pt. 1—Metals—p. 840, Fig. 10b (1936).

DISCUSSION

Presiding: WM. ROMANOFF, H. Kramer and Co., Chicago, Ill.

Co-Chairman: W. W. EDENS, Ampco Metals, Inc., Milwaukee, Wis.

MEMBER: Has any work been done to tell us why the lead apparently has a detrimental effect upon these alloys? If such work has been done, and we know why the lead has this effect, would it be possible to use some counterirritant to neutralize the effect of the lead? According to this paper, adjusting the copper will compensate to a certain extent, so I was reminded of my early experience with red brass, when we found that silicon was very bad, as it still is, but that the use of nickel will cover it up. I do not say it corrected it, but it acted as a counterirritant to both silicon and lead to a limited degree, and I was just wondering if possibly some work along that line had been done on manganese bronze.

MR. HALLIWELL: In the first place, lead does not form a homogeneous alloy with copper and zinc but is always precipitated as discreet particles; in other words, it does not form a solid solution. Now you might reply, "Neither does iron." Iron, however, is found alloyed as a hard compound. It increases the resistance of the metal crystals to slip, and resistance to slip (deformation) means increased tensile strength and frequently increased elongation.

Lead is present as a soft material both within the crystals and at the crystal boundaries. Any material, soft or hard, which is located at the crystal boundaries is not conducive to high ductility or high tensile strength. Therefore, the composition of the base alloy must be such that the inherent ductility is increased to counteract the evil effects of lead at the grain boundaries or along the slip planes of the crystal. The increase in copper content increases the amount of the alpha phase present, which, being more ductile than the beta phase, increases the elongation of the alloy.

B. A. MILLER[1]: Mr. Halliwell states that, in order to use lead up to 1.5 per cent, the copper content must be increased. I think he should go a little further and indicate what that does to the yield point.

MR. HALLIWELL: The yield strength decreases with an increase in copper, irrespective of the lead content, and is accompanied by a decrease in hardness and tensile strength and an increase in ductility.

CHAIRMAN ROMANOFF: There is an interesting point that Mr. Hesse brought out, about which I am sure most consumers and producers of manganese bronze will wish to know.

I understand that there is a specification being formulated, or possibly it is out at the present time, that will require a minimum of 0.5 per cent of tin. In other words, a limit of 0.5 to 1 per cent will be specified. Now, most of the material available, which is necessary for low-lead alloys, will not contain much tin. I would like to know about the dezincification test, or just how long it will take for some of this manganese bronze to actually dezincify. If tin is necessary, it can be added,

[1] Cramp Brass and Iron Foundry, Div. Baldwin Locomotive Works, Philadelphia, Pa.

and that is not difficult. I understand that tin is a strategic material, and its use will increase the cost. Can anyone elaborate upon that? In other words, I would like to know how long it will take for some of these manganese bronzes to dezincify.

MR. HESSE: I am afraid I cannot answer that question. However, I might repeat that 0.25 per cent tin in manganese bronze seemed to be an effective amount for resistance to dezincification. Studies on alloys containing as little as 0.10 per cent tin are in progress with a view toward the conservation of the strategic element, tin.

H. J. ROAST[2]: We have made manganese bronze for naval purposes and the question was recently raised by us, as by you, as to whether the tin was necessary. A navy commander, who was in my office, stated that in his experience 0.25 per cent of tin would very definitely decrease the dezincification under salt water conditions, where salt water could remain in contact for some period, as in the rings of certain guns, fittings, et cetera, when they are immersed or waves are washing over them. So, it was his opinion that the addition was a definite improvement.

I would like to ask two questions of the author: Were the bars round or square, and has any work been done on the effect of lead, associated with the tin or without it, on the dezincification problem? That is a question that is to the fore in Canada again at the moment. Does lead have any effect by itself on dezincification, or is its association with the small quantity of tin necessary?

MR. HESSE: We have a few tests under way in which we are studying the effect of lead on the resistance to dezincification of tin-containing and tin-free alloys. Lead does not seem to affect the resistance to dezinicification of the tin-free alloys.

Likewise, lead and tin in combination in these alloys do not seem to influence the resistance to dezinicification.

MR. MILLER: Mr. Hesse's paper seems to carry out a little experimentation that we did at our plant, and I was pleased to hear Mr. Roast's comment upon the problem of dezinicification. He also spoke of the salt water contacting the various parts and causing dezincification. I feel we had better go very slowly on the leaded manganese bronzes in propeller wheels. I appreciate that the metals are urgently needed and that substitutions must be made, but I know, and I think Mr. Hesse will agree, that the Navy feels we should proceed cautiously on leaded manganese bronzes.

CHAIRMAN ROMANOFF: I do not think anybody wants to manufacture a manganese bronze that will not meet everything desired. All I want to impress is that there are certain materials which are going to be available and I know, in speaking of losing a propeller blade, that the Navy did go to gray iron on landing barges and certain Liberty ships. Now, are we going to make things today that are going to last a lifetime, or are we going to make things that are going to last for the "duration" or a little longer, and make them with what we have? We are not trying

[2] Canadian Bronze, Ltd., Montreal, Canada.

to find any fault; I do not think any producer of ingots, for instance, is finding any fault with what they should make, simply because we would like to use very low lead and any tin desirable, if we can get the available material. I just wanted that clear, lest there is any misunderstanding.

MR. MILLER: The only reason for my remarks was to indicate that we should go slowly. I know that there are a number of people present who are just beginning to break into the manganese bronze manufacture and we, with a few years of experience, are attempting to convey to those particular people that it is a new development and they should proceed cautiously.

CHAIRMAN ROMANOFF: You are right, but we must set our pace to the demand.

MEMBER: I do not think anyone can prophesy what we may be called upon to use for materials in the future. I am referring to Mr. Roast's remark about Canada, calling upon the foundrymen to use manganese bronze as a substitute for the unavailable red metals. I should like to forestall some of the difficulties that we are talking about with this tin. I think it would be very valuable, while making a study of the benefit of tin additions for dezincification, to make some study, also, as to what that would do if we are called upon to make castings that are required to stand pressures.

We have not done a great deal, but on the first few tests, where we were required to add tin in a manganese bronze that had formerly been able to withstand very successfully pressures of 900 lb., the addition of 1.5 per cent of tin would not hold 500 lb. pressure. Therefore, I hope that, in addition to the work that is being done to determine the beneficial effects of tin for dezincification, a study may also be made as to what effect that has upon its ability to withstand pressure.

MR. HALLIWELL: With reference to Mr. Miller's remarks concerning yield strength, it will be noted from the curves that the alloy containing no lead drops its yield strength from 30,000 lb. at 57 per cent copper to 24,000 lb. at 60 per cent copper. With the one per cent lead alloy, the maximum occurring at 57 per cent copper is higher than the non-leaded alloy by about 2500 lb., and at 60 per cent copper is less than 500 lb. lower.

Regarding tin, the specification that Mr. Romanoff mentioned, of a 0.5 minimum, there was no mention of a maximum.

CHAIRMAN ROMANOFF: Yes there is; one per cent.

MR. HALLIWELL: I was going to say, if there was no mention of a maximum, you have to be very careful when you go above one per cent that you do not get the brittle delta constituent in your material. That may be partly the reason for not standing up under pressure, because that is a hard, brittle material and there is a certain amount of internal shrinkage around the delta constituent as it solidifies.

MR. MILLER: Mr. Halliwell, I think you have done a very good job, and you are deserving of a lot of credit but, at the same time, do not lose sight of the fact that in the propeller wheel the tensile properties are quite different than they are in any test bar.

MR. HALLIWELL: I think we are missing one point which Mr. Hesse has brought out. I do not think it is yield strength or tensile strength, or ductility—I think it is impact.

MR. MILLER: I agree with you 100 per cent, and to further that particular statement, we ran a series of leaded manganese bronze tests, and as lead went up to the neighborhood of 0.6 per cent we had test bars break into prick points. If that is not indicative of low impact value or low strength, I do not know what is.

MR. HALLIWELL: You probably have to be more careful of notch sensitivity with the higher lead alloys, because any irregularity on the surface will cause higher notch sensitivity.

CHAIRMAN ROMANOFF: There is quite a lot of manganese bronze experience here. I wish some of you people would get up and help some of the foundrymen who really need some help in manganese bronze today.

MEMBER: Manganese is a difficult metal to work with. Temperature is one thing that has not been stressed at all. If I am wrong, I should like to be corrected, but it is my contention that in some cases 25° in temperature on manganese bronze represents the difference between a good casting and a poor one.

MR. HALLIWELL: I would not put it as low as that, more likely 50°.

MEMBER: Has it been taken into consideration that the pyrometer, which has been referred to casually, is the most important item a foundryman can have? We have gone into places, running 20 per cent scrap, and have cut down below 2 per cent by just one thing. We have taken a reading when the metal comes out of the furnace; we take a reading when it leaves the skim pole; and then we take a reading just before it goes into the mold. Those readings are written down as the temperature of the foundry for that day.

A man pours two or three small castings and, when he gets into his large casting, his minimum heat should be 1800°F. Perhaps it is 20° outside and, if he has three large castings to pour, by the time he is pouring the last one it is down to 1750°F. If he will make out a deviation card, recording temperatures that are giving perfect success and determining the change in those temperatures, that is, the change in the elements, and gauging his pouring accordingly, he usually has one successful casting after another.

MR. MYSKOWSKI: I would like to point out that, in some of the work we have done, our results agree quite well with Mr. Halliwell's insofar as the yield strength is concerned. There has been just a very slight reduction in the yield strength with the addition of lead in most cases, probably of the order of 2000 lb. per sq. in.

Another point that was presented was the matter of pouring temperature. The pouring temperature is not important up to a certain point. If we get our castings too hot and our temperature too high (I am speaking mainly of test bars in this particular case) and, if we go to a temperature around 1850°F., fairly close to flaring temperature, our results will not be nearly as satisfactory as they might be. For instance, the tensile strength will probably drop off 2000 lb. per sq. in.

and the ductility will suffer somewhat, but in our own work we have found that, in a pouring range between 1832°F. and about 1740°F., there has been absolutely no variation in tensile strength and elongation. The tensile strength will be consistently 77,500 plus or minus 500 lb. per sq. in. The ductility will run around 30 per cent plus or minus one per cent in 2-in., which is really as close as we can expect. Actually, we get a pouring range of plus or minus 50°F., which is not too close for the controls one must observe.

We might have to watch out for the addition of tin. As mentioned, the addition of tin will compensate for the copper and zinc present, but it might cause an introduction of the so-called delta constituent. The effect of tin is quite pronounced. The addition of tin, even with the copper-zinc ratio constant, will increase the tensile strength and reduce the ductility. If we indiscriminately add tin, we will find the tensile strength will go up quite fast, for the reason that 1 per cent of tin is equivalent to about 2 per cent of zinc when added to manganese bronze, and with such a condition we will find the copper-zinc ratio is off considerably.

Actually, the structure of your brass is changed appreciably. For instance, starting with a 59 per cent copper alloy, with about 38 per cent zinc, with no tin present, we probably would get about 50 per cent alpha in the microstructure. The addition of one per cent tin greatly reduces the amount of alpha. We would probably finish with about 10 per cent. With this change in structure a change in properties is to be expected.

Now, while tin is very efficient in preventing dezincification, if we are going to add it, we must watch out for our composition and keep the ratios of our structure very well adjusted. We will show how to obtain this copper-zinc ratio; in other words, given the composition or given the copper-zinc ratio, how to figure back. Of course, we try to keep the minor elements within rather narrow limits. We have found, for instance, that the presence of 0.6 iron instead of 1 per cent iron cuts down the tensile strength quite a bit as well as the ductility. That was an isolated instance and it might bear more checking into but, as we say, this paper principally has covered bronze, with the minor elements in a very narrow range.

H. R. KING[3]: I would like to ask Mr. Miller, regarding the use of tin in manganese, just how necessary it is to prevent dezincification. How long does a propeller last in salt water, and what is the need for tin to prevent dezincification?

MR. MILLER: I am unable to answer that because there have been no adequate tests, except that I think Mr. Hesse is pioneering in that move but, from the mere fact that dezincification is another form of the notch test, I am inclined to be "leary" of it. I do not know the actual time when the dezincification will have caused a rupture. It is just playing safe, that is all.

MR. KING: If it has not been necessary in the past, I am wondering

[3] Metal and Alloy Specialties Company, Inc., Buffalo, N. Y.

whether it would pay to add any tin to manganese bronze just for that purpose. If manganese stands up in salt water fairly well, perhaps we do not need to consider it and, therefore, we do not have to worry about lowering physicals.

MR. MILLER: I have seen propeller wheels which had no tin present at all, in which dezincification was very pronounced, but I would not be in a position to make any commitment as to their length of service.

J. F. EDNIE[4]: First of all, I think the authors are to be congratulated for the splendid manner in which they have presented, from a metallurgical point of view, the effects of lead in manganese bronze. Particularly are we pleased with the inclusions of yield strength data at 0.5 per cent elongation. Specifically, what we mean is simply this: It has been the common practice in the past to report only the ultimate tensile strength and per cent elongation for manganese bronzes—in fact, most specifications require only these two figures. Such a specification inferentially places undue emphasis upon these two factors alone. Anyone who knows what a stress-strain curve looks like for a manganese bronze tensile test will readily appreciate that the yield strength at 0.5 per cent elongation is a much more important value to consider when appraising the relative merits of a particular alloy.

From an engineering standpoint, I should think that the ultimate strength of manganese bronze has very little meaning, for the simple reason that by the time a typical bar of manganese bronze has reached its ultimate strength of, let us say, 80,000 or 90,000 lb. per sq. in., the metal has stretched 30 or more per cent in tension. In other words, it is very unlikely that any piece of equipment can permit of 30 per cent elongation in one of its members and still remain serviceable.

We have been doing a little experimentation in the past year or so with a manganese bronze containing around 20 to 25 per cent zinc. This bronze has certain desirable properties, principally its decreased fuming characteristics when melted in the foundry. As everyone knows, many foundries object to the use of manganese bronze because of its excessive fuming, which requires special ventilation and hooding. The bronzes we have worked with show very moderate zinc loss as well as very reasonable mechanical properties. In charting our stress-strain curve, it is drawn on a reduced scale so that we obtain an apparent proportional limit value on the curve. A comparison of this value with the conventional ultimate strength for other bronzes reveals some very interesting facts; for example, when we compare our low-zinc manganese bronze with a typical 85-5-5-5 alloy we find that on the basis of ultimate strength and elongation alone they are somewhat equal—the manganese bronze always a little better. However, from the standpoint of proportional limit comparisons, the manganese bronze value will be 2 or 3 times that of the 85-5-5-5 alloy. If this means anything it means that, from a structural standpoint, the manganese bronze alloy is definitely superior. Of course, this consideration completely ignores other

[4] Duquesne Smelting Corp., Pittsburgh, Pa.

important factors, such as corrosion resistance, impact values, frictional wear, and etc.

The results of our experiments indicate that the element which has the greatest effect on the yield strength is manganese. We have obtained 50 per cent greater yield strength by doubling the manganese content of the alloy.

In conclusion we would like to add that as far as lead is concerned, all of the alloys we have experimented with contained at least one per cent lead and as high as 3.5 per cent. There is no question but that its greatest effect is on the ultimate strength and elongation, particularly when dealing with high tensile bronzes. This, of course, goes right back to our opening remarks—that the ultimate strength is not the true value to use in attempting to appraise manganese bronzes.

Job Analysis and Time Value Predetermination as Applied to Patternmaking

By W. D. Walters*, Pittsburgh, Pa.

1. Regarding patternmaking in general, we think of each pattern as being different and non-repetitive. Therefore, many pattern shop managers assume that it is impractical to measure patternmaking, as is done in the case of productive operations in the foundry.

2. However, when we classify patterns by types, we find there is a lot of similarity between those in the same group. On this basis, it has been proven possible to predetermine time values for an unlimited range of pattern work.

3. The patterns made at the Westinghouse Electric Company in Trafford, Pa., vary in size and type in proportion to the widely diversified products manufactured by the company. Time values for making these patterns range from 1 to 6,000 hours, and predetermining of time values as a basis of wage payment and pattern cost has been in effect in this shop since 1920.

Procedure

4. Patterns lend themselves to classification by common types having the same general characteristics, differing only in size or some details of design, such as, openings, ribs, bosses, etc. Examples of different classes are: Motor frames, gear cases, bearing brackets, pedestals, contact feet, and others. Any of these can be subdivided into specific types of each, as the activity warrants.

5. With the patterns thus classified and a reference file, composed of cards as shown in Fig. 1, representing patterns of a range of sizes and different characteristics within each class, the proce-

* Supervisor, Time Study, Westinghouse Electric and Mfg. Co.

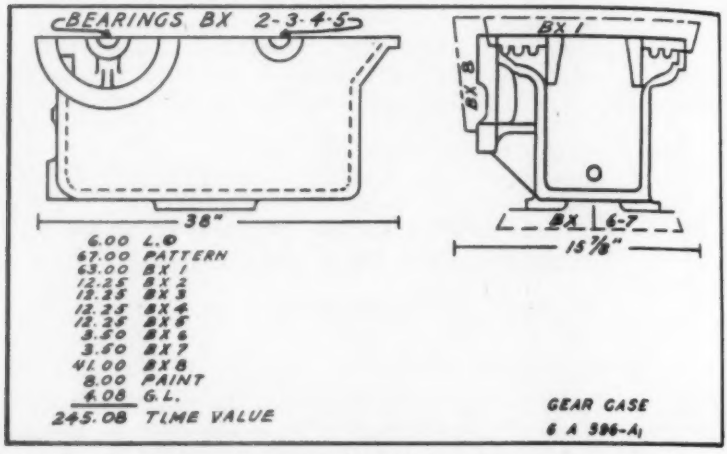

FIG. 1—SHOWING CLASSIFIED REFERENCE FILE CARD FOR PATTERN. LEGEND: BX—CORE BOX. L.O.—LAYOUT. G.L.—GROUP LEADER. CORE PRINT GENERALLY IN RED.

dure for determining a time value for the making of a pattern is as follows:

(1) The job is analyzed to determine its size and various characteristics.

(2) Referring to the section of the reference file covering the specific class of patterns, the job comparing the closest with the one in question is found.

(3) With the time value of the job from the file as a basis, allowance for minor variations between the jobs is added or subtracted to arrive at a time value for the job in question.

6. Using the time values in the reference file as a basis minimizes the amount of estimating and maintains consistency which would not be possible without such a guide.

7. Also, time values for cost estimates, determined in this manner, will be the same as the values allowed on the subsequent orders.

BUILDING THE REFERENCE FILE

8. The range of size and variety of the work will generally limit the extent to which patternmaking time values can be determined by time study, considering the amount of time required per study. However, from a number of time studies of small and

medium jobs and close checks of performance and time on large jobs, the relative past performance efficiency can be determined. With that known, past performance records can be adjusted and used along with the time study and check data as a basis for determining time values for the reference file.

9. By plotting the past performance time along with the time study or checked times, as shown in Fig. 2, abnormal times will stand out, thus eliminating themselves from consideration. Of course, it is necessary to analyze the jobs involved to determine whether the time is justifiably out of line, due to difference in their characteristics.

10. A man selected to build the reference file or to determine time values with the aid of it should be one who has had several years of patternmaking experience, in addition to his apprenticeship, and good analytical ability, since a considerable amount of judgment is required in determining the amount of allowance for the many variations in patterns. In addition to these qualifications, of course, some time study training and guidance will be necessary.

ADMINISTRATION

11. The following figures will give an idea of the cost of predetermining time values in the manner described:

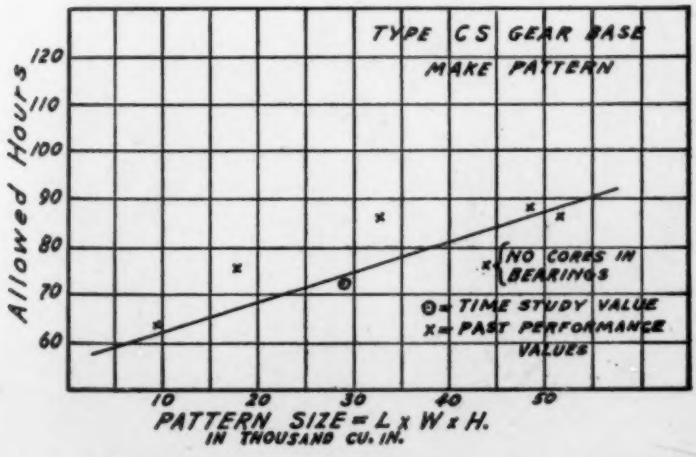

FIG. 2—GRAPH SHOWING PAST PERFORMANCE TIME PLOTTED WITH TIME STUDY VALUE FOR DETERMINING TIME VALUES FOR REFERENCE FILE.

12. In the Westinghouse pattern shop, previously mentioned, which employs 125 patternmakers, two men predetermine the time values for all pattern orders, changes and cost estimates. An average of 60 jobs are handled per 8 hr. day, including extra allowances for unusual conditions not considered in the original time values.

13. The original reference file has been added to, over the period of years, till it is now composed of approximately 5,600 cards, divided into about 70 different classes of patterns.

14. The sketches are free-hand and show only enough detail to give a good conception of the job. On other than simple patterns, the marked drawings, which give a full description of the pattern, are referred to for detail comparison.

15. In the case of metal pattern molding and finishing, the work is of a more repetitive and less variable nature than the wooden patternmaking. Therefore, that work lends itself to formulizing, as is commonly done in the case of core making, molding, and other repetitive operations in the foundry.

DISCUSSION

Presiding: VAUGHAN REID, City Pattern Works, Detroit, Mich.
Co-Chairman: FRANK C. CECH, Cleveland Trade School, Cleveland, Ohio.

C. HENNING[1]: May I ask Mr. Walters if he has any base rates in case they do not make a reasonable amount under this plan?

MR. WALTERS: Yes, in case they do not meet the allowed time, they are paid their hourly rate. In other words, they have one hourly rate. If one of the groups falls down, as we refer to it, for the pay period, they are paid their total elapsed time.

MR. HENNING: On some of these you evidently have a criterion upon which to work, where you make a pattern similar to the other, only different in size. However, supposing you have something entirely different, how do you come to a conclusion on that?

MR. WALTERS: When we have something entirely different, by breaking it down into different parts, sections, and etc., we can always find something with which to compare a certain amount of it. We attack patternmaking like any other job, by breaking it down into detailed parts, determining time for the various parts, and then building up the time for the whole job.

MR. HENNING: Then how do you conclude the class of pattern the man is going to make, whether he is going to nail up a box, screw it up, or how he is going to build it? There are many ways of making a pattern.

MR. WALTERS: We have our patterns graded, depending upon the activity.

MR. HENNING: It is mostly a one-casting job?

MR. WALTERS: No, the activity varies widely. For low activity, of course, we have a lower grade of pattern, and as the activity increases the grade of pattern increases in proportion.

[1] J. I. Case Co., Racine, Wis.

Report of Committee on Methods of Producing Steel for Castings

Some of the important developments of this subject, dealing principally with melting, alloying, and deoxidation practices, long in the making, have only recently reached fruition.

Cast armor has been an outstanding success, due, partly, to the willingness of foundrymen to venture into strange territory. Much credit, however, must be given to the spirit of cooperation which influenced all producers to pool their information for the common good.

Before the advent of cast armor, the technology of armor plate was static and, one might say, almost archaic. The widespread cast armor development has shown that various compositions are suitable, when properly processed, and that satisfactory steel for armor can be made by both acid and basic practices, in either electric or open-hearth furnaces.

The side-blow converter seems about to be legitimized as a creditable member in the family of steel-making apparatus. Long stigmatized as a crude and inferior method of producing steel, the quality of converter steel has been shown[1] to be on a par with cast steels made by other processes. The importance of such a recognition is emphasized by the fact that our war effort has demanded an increased capacity for steel castings, and the use of converters, in conjunction with already available cupolas, is the quickest and cheapest method of expanding such capacity. The successful adaptation of the "electric eye" to the side-blow converter has greatly facilitated control of composition.

The Sesci type of rotary fuel-fired-furnace, which is similar to the Brackelsburg furnace, has apparently had some measure of success in England. One excellent account[2] describes operations in a 5-ton furnace, fired with both powdered bituminous and anthracite coal.

In operation the furnace is charged by a standard type of ground-charging machine, when as much as 1500 lb. of turnings

1 Superior numbers refer to references at end of this paper.

NOTE: This paper was presented at a session on Steel Castings at the 47th Annual Meeting and Second War Production Foundry Congress, St. Louis, Mo., April 30, 1943.

may be charged. To the scrap, sufficient anthracite or pig is added to produce a good boil after melt down. During the melt down, an occasional partial rotation is given to produce uniform heating, but too much rotation at this stage would result in mechanical damage to the furnace. After about an hour, when the charge becomes pasty, the full rotational movement is applied.

After melt down, the operation is very similar to the acid open hearth practice. Alloys are added to the furnace and deoxidation with Alsimin is carried out in the ladle. The furnace is tapped from the side, or periphery, of the barrel.

The lining is made up of graded quartz bonded with 15-35 per cent of clay material and 6-7 per cent of water to make a rammable mixture. The present record for heats, per lining, is 226.

Data are given to support the claim that the quality of the steel produced is on a par with steel produced by any other process. No discussion of the comparative economy of this method was given.

It may surprise some to learn that good quality steel can be produced in a Sesci type of furnace, but it seems apparent that there are certain fundamental principles which, when fulfilled, will give high quality steel regardless of the type of melting apparatus. The nature of the raw materials, the scale of operation, power and fuel supplies, and the end product may dictate the kind of apparatus that is most expedient, but the same principles apply to all.

Two of the principles in preparing steel for castings, to be more firmly established each year, are (1) the desirability of an oxidizing period or boil in each heat and (2) the undesirability of holding steel in the deoxidized condition for prolonged periods.

In making additions of deoxidizers to the ladle, only that part which gets into the steel counts. This trite statement is prompted by the same recent experiences in various plants, which show that the addition noted on the heat record may bear little relation to the actual deoxidation. These experiences show that, unless considerable care is exercised, much of the alloy addition, especially such a light metal as aluminum, may be lost in the slag. Naturally, the expected results are not then realized.

Much concern is being felt over the increasing contents of sulfur and phosphorus in available scrap. The acid melters particularly are being pushed to the ragged edge of specifications, and much thought is being given and considerable experimental work is under way on means of circumventing these ubiquitous elements.

Some plants are changing to basic practice because of sulfur and phosphorus.

In the cupola-converter practice it is standard to desulfurize the cupola iron with sodium carbonate before blowing. The Yocum[3] process for dephosphorization is being successfully applied to some extent but principally on converter steels. Other methods in the experimental stage have not yet been published and cannot be discussed here.

For more complete reference to published work on this subject, the members of the Steel Division are urged to make use of the abstract service in the *American Foundryman*.

COMMITTEE ON METHODS OF PRODUCING STEEL FOR CASTINGS

C. E. SIMS, *Chairman*
J. B. CAINE
A. W. GREGG
ERNEST LANCASHIRE
CHAS. LOCKE
F. A. MELMOTH
W. HARVEY PAYNE
H. D. PHILLIPS
W. J. PHILLIPS
E. C. TROY
R. C. WOODWARD

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3. YOCUM, GORDON, "*A Method of Rapid Dephosphorization of Bessemer Steel*," METALS TECHNOLOGY, p. 1265, January, 1941. Also see American Institute of MINING AND METALLURGICAL ENGINEERS, IRON AND STEEL DIVISION, vol. 145, pp. 160-174 (1941).

DISCUSSION

Presiding: C. W. BRIGGS, Steel Founders' Society of America, Cleveland, Ohio.

F. A. MELMOTH[1]: I had the good fortune to be in England on a visit when they started the plant which Mr. Sims has used as an example of the system, and I spent several days in the plant. At first there were a lot of difficulties, and you may be assured that I was very interested in reading the article which appeared in the *Foundry Trade Journal*, stating how the problems were overcome.

Even more interesting was a letter I received from my friend, Paul Fassotte, who, as you know, has done more to develop the Sesci than any other man over there. He tells me that they now have developed a method which gives indication of being very useful. They are melting in a Sesci type furnace and producing a highly superheated high-carbon, low-silicon material, and blowing it in a converter, with a production of steel having remarkable qualities, particularly from the standpoint of cleanliness.

As a member of Mr. Sims' committee, I was not able to get the information to him because I received the letter only last week, but I have been successful in getting a copy of the patent.

A factor which is going to greatly influence its value is the possible development of a basic lining for the Sesci, which will open up considerable possibilities from the standpoint of the converter. If they can melt a high-carbon, low-silicon material, superheat it in the Sesci and remove some of the impurities, doing away with several of the presumed deficiencies of the converter, it might be possible to get a steel which is quite comparable to electric steel at its best.

CHAIRMAN BRIGGS: What was the raw material being used in the Sesci?

MR. MELMOTH: Steel scrap and pig iron.

MR. SIMS: One point which I endeavored to bring out in the report, without dwelling upon it too long, is the fact that all steel-making has certain common fundamental aspects. The particular apparatus in which the steel is made may be of little consequence as long as these certain fundamental conditions are observed. In going through a dozen plants as many as a dozen different practices may be observed, with all of them making acceptable steel.

Therefore, it must be concluded that all of the things which are being done are not important. Some of the operations may be regarded, in any one plant, as being vital, but if the other fellow gets along without doing them, it seems to be prima facie evidence that they are not of extreme importance. However, one thing is obvious—throughout all steel-making processes there are certain principles in common; there is some sort of common divisor that runs through them all. These indicate which are the important phases, and as long as those conditions are fulfilled, it makes little difference what the apparatus is called.

[1] Detroit Steel Casting Co., Detroit, Mich.

Direct-Arc Electric Furnace Refractories

By E. K. Pryor*, Cincinnati, O.

Abstract

Increased use of direct-arc electric furnaces in recent years and the severe operating conditions of the present time have made acute the problems of selection and development of proper refractory materials. In this paper, the author covers the various causes of refractory failures and shows the application of refractory products to direct-arc electric furnace practices. Properties of refractories and their relation to furnace operating conditions are discussed. Methods of furnace lining design and construction are presented. Necessity of proper design of door arches, jambs and roofs, and the selection of proper refractories for these points is particularly stressed. Performance of masonry type linings and monolithic linings of the sillimanite type are shown. Co-operation between operators and refractory manufacturers is suggested as a means of improving refractories now in use and the development of new refractory materials.

1. The direct-arc electric furnace has been gaining favor rapidly in both steel mills and in steel foundries during the past decade. Increased production of both ingots and castings by direct-arc furnace methods, during the past 4 years, has been most remarkable. Impetus from the war effort has been marked. This trend was established by the demands of modern engineering design which so frequently call for special steels and their applications very near the ultimate limits. These demands will continue after the war and will mean a continuation of proportionally great electric steel production.

2. It is probable that refractories give the operators of direct-arc electric furnaces more concern than any other vital part of the equipment. Refractories used include standard brick, special shapes, plastics and aggregates. In some instances, acid, basic and neutral material are all used in the same furnace unit. No truly adequate brick or refractory has been developed, particularly for use above the metal line.

3. As hearth refractories give relatively little trouble in most plants, no attempt will be made to cover this phase. It is sufficient

* The Charles Taylor Sons Co.

Note: This paper was presented at a session on Foundry Refractories at the 47th Annual Meeting and Second War Production Foundry Congress, April 30, 1943.

to say that for both acid and basic practice, hearths are fairly well standardized and vary but little from plant to plant. Essential construction details are similar. The initial preparation of the hearth is probably the important factor. If this is done properly, repairs and maintenance will be comparatively simple. With proper care, either acid or basic bottoms will last almost indefinitely.

4. Roof and side walls cause the operator the greatest concern. Failures of both are due to the following causes:

1. Chemical attack by slags, dusts and fluxes.
2. Thermal and structural spalling.
3. Mechanical spalling, due to bad masonry work, and to poor design in the case of roofs and arches.
4. Low softening point compared to temperatures involved. This may affect only spots, or be general.
5. Mechanical abuse.
6. Insufficient hot strength.

5. Selection of a refractory for any high temperature furnace is essentially a compromise. Choice of the type and grade of refractory to be used for a given job is based on its proved ability to overcome one or more of the destructive factors listed. When early failure of the refractory is limited to only one of the points listed above, the condition may be corrected by a change to another suitable refractory material or by taking precautions in the operation of the furnace. An example would be the case of severe spalling of silica brick due to heat shock. Either the rate of heating and cooling must be changed or a super-duty or mullite-type brick must be used. Mechanical spalling due to poor masonry work can be readily corrected. Where the condition is the result of poor design, however, the correction is more difficult. Frequently, mechanical abuse, either through negligence or as part of an operating condition, is a contributing factor in refractory failure. The reasons for failures are not always apparent and it is well to consider all the factors involved before arriving at a conclusion or making elaborate changes.

SILICA REFRACTORIES

6. Silica refractories have been the predominant material used in the roofs and side walls of direct-arc electric furnaces. In basic operations, they are gradually being displaced by other refractories

in side walls, door arches and jambs; but roofs of silica brick are still the most generally used. In acid practice, silica brick is almost universally used; the exceptions being found most frequently in units of 1000 lb. per hour capacity and less.

7. The most outstanding characteristic of silica refractories is their excellent load-bearing capacity at high temperatures. They will withstand abrasion reasonably well. Their fusion point will average very close to 3100°F. Their resistance to slag attack is low; although they will withstand attack by FeO with less damage than any of the alumina-silica refractories. Basic slags, containing both CaO and FeO, will attack silica quite rapidly. The susceptibility of silica brick and shapes to thermal shock, and resultant spalling, is a great hindrance to their giving acceptable performance. The effect of this inherent characteristic is most pronounced when the operation of the electric furnace is intermittent, and has an adverse effect on door arches and jambs, even when operations are continuous.

Causes of Side Wall and Skew Failures

8. "Hot spots" develop frequently in both the roof and side walls of electric furnaces lined with silica refractories. At times, this can be remedied by slight changes in the electrode positions. Instances have been reported where "hot spots" have developed in the side wall, generally adjacent to the electrode mast. The use of a panel of magnesite or chrome-magnesite brick in this position often will overcome the difficulty. This panel may, or may not, be separated from the silica brick by a course of neutral chrome brick. The use of such a panel is not practical in roofs.

9. There is frequently a tendency toward excessive wear on the face of the last course or two of side wall brick, where the roof ring and skews meet the side wall. This probably is due to currents of furnace gases rising and changing direction at this point as they flow toward the electrode openings. These gases carry a considerable burden of metal oxides and fluxes which naturally have a good opportunity to attack at this point. This action itself is difficult to remedy but the resulting skew failures may be retarded, the writer feels, by slight changes in design of the side walls at this point, as well as possibly the skews. If the top three courses of side wall brick are brought out into the furnace by a corbel so that the skews are protected from the direct action of the gases and their burden of fluxing materials, as well as from the direct radiation of the arc, some improvement of skew life should be ap-

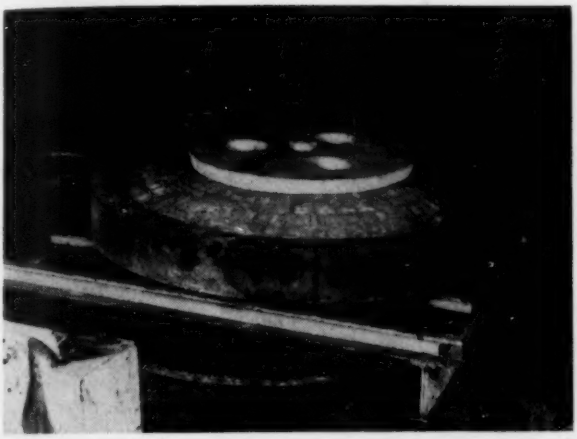

FIG. 1—THREE-TON DIRECT-ARC ELECTRIC FURNACE ROOF CONSTRUCTED WITH SPECIAL SHAPES 12-IN. THICK FOR CENTER PORTION. ANNULUS IS OF 9-IN. SERIES BRICK.

parent. Certainly, repairs to the side walls are accomplished more readily than any attempt to bolster a failing skew.

10. Designers of electric furnaces have been improving the design of the roof rings and using more adequate water cooling systems for the skews. This has been helpful, but generally speaking, the temperature differentials obtained between the hot and cold face of the refractories have not been sufficiently improved to be of any great importance until a point is reached where skew failure is imminent.

Causes of Roof Failures

11. Roof failures around the electrode openings probably are due most often to the flow of gases out of the furnace at this point, because of the stack effect of these openings. As pointed out, these gases usually will carry an appreciable burden of entrained dust and vapors which, coupled with the radiation from the arcs, will have a very definite fluxing action on the refractories forming the electrode openings. Failures at this point most often are laid to spalling, implying thermal shock. While there is no question but that thermal shock does play an important part at times, the writer feels that more frequently spalling is due to the formation of new compositions at the face of the original refractory by reactions with slags and fluxes. These new compositions will be of different specific volume. The result is quite similar to thermal shock, but the remedy different.

12. The center section is frequently made thicker (Fig. 1) than the rest of the roof. The object of this is to extend the life and balance the service of the center with that of the outer portions of the roof. Quite often this objective is not attained. Some operators have had fair success in patching around the electrode openings, but generally speaking such efforts have not been satisfactory.

13. A few operators have attempted to bolster the life of the electrode openings by forming them of sillimanite brick (Fig. 2). Where thermal shock is the predominant factor in the early failure, there is merit in the idea. Otherwise it would appear that sillimanite could offer little advantage. One operator of a 3-ton per hour, rated capacity, furnace has used a complete center section of sillimanite and quite definitely improved the overall roof life with good economy. However, more experience is required before definite conclusions may be drawn concerning such applications.

SILICA MORTARS

14. Most operators of direct-arc electric furnaces lay their silica refractories without mortar, mainly because of poor service that they have encountered with mortars. Silica mortars are of little benefit unless they have a P.C.E.† that is equal to or

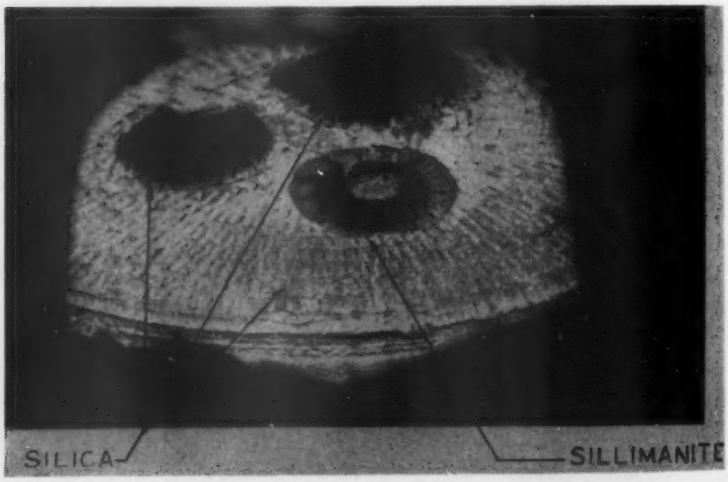

FIG. 2—RESULTS OF COMPARATIVE TEST OF ELECTRODE OPENINGS FORMED OF SILICA AND OF SILLIMANITE BRICK AS APPLIED ON ROOF OF 7-TON DIRECT-ARC BASIC ELECTRIC FURNACE PRODUCING ALLOY STEELS.

† Pyrometric Cone Equivalent.

greater than that of the silica brick. If the P.C.E. is well below that of the silica brick, the mortar runs out at joints quite readily at temperatures found in direct-arc furnaces. Special silica mortars are available having a P.C.E., for practical purposes, equal to that of the best grades of silica brick. These mortars are heat setting, the ceramic bond developing only after the abrupt expansion of the silica. Almost invariably, the use of such a mortar will pay dividends in the form of increased service from silica refractories in arc furnaces.

15. The use of the above described mortar will lessen the effects of thermal shock by holding in place pieces of brick that have spalled, protecting the new surfaces formed from direct attack by the various factors of destruction. Its use will lengthen the life of door arches and jambs quite definitely by reducing the effects of thermal shock. The displacement of individual brick in the arch and jambs, by ramming during charging, is pretty well eliminated since the ceramic bond developed will prevent the dislodgment of individual brick.

16. Silica brick and small shapes often slip in the roof and are rapidly burned off, resulting in a weak spot or "sting out." If the proper mortar is used, such conditions almost surely are eliminated. Likewise, such a mortar will reduce joint erosion and corrosion quite effectively and will prevent the infiltration of gases and the penetration of fluxes. At the same time, the mortar seals the joints and restricts the action of heat and other destructive factors to one face. If no such mortar is used, five faces of the brick are open to attack.

17. Normal allowance for expansion is made when using such a mortar. Where spacers are placed in a joint, the mortar is used on only one side of the joint. When the spacer has burned out and the adjoining faces meet, a ceramic bond will develop between the two faces.

18. It generally is difficult to convince operators that any appreciable advantage is to be gained in the use of mortars with silica masonry. But almost invariably the extra initial expense, and additional expense of application of the proper mortar, will be more than offset by the results obtained in service. It will result in uniform bearing and stress distribution between individual silica brick or blocks, allowing a minimum of structural failures.

It will give essentially the benefits of a monolithic refractory mass, yet will not cause spalling or structural cracks to travel from one point to the limit of the mass as would be the case in a single large silica shape.

BASIC AND CHROME REFRACTORIES

19. For some time, the use of basic refractories in basic operations was limited to a point just above the bath level, where protection against slag erosion was necessary. Magnesite refractories have excellent resistance to basic slag attack and a much higher P.C.E. than silica refractories. Their resistance to spalling is somewhat better. Unfortunately they expand to a greater degree than do silica refractories, and have a very poor load capacity at high temperatures. Much improvement has been made in recent years, overcoming, to a degree, these weaknesses. The manufacture of low-iron magnesite brick, and the use of higher burning temperatures have helped to make a more acceptable brick. However, the introduction of unburned, chemically-bonded magnesite brick and brick of the chrome-magnesite type, has probably done more than anything else to extend their use in direct-arc furnaces. Likewise, metal-cased, basic brick has found favor. Their susceptibility to attack by siliceous slags and drippings has not been overcome. Reducing slags generally contain enough silica to adversely affect basic side walls. However, magnesite and chrome refractories are proving their worth in many operations where extreme temperatures, severe operating schedules and attack by basic slags and fluxes have proved too great for silica.

Use of Basic Brick

20. The use of basic brick has been limited to side walls. Experiments with basic roofs have not been encouraging to date. Both metal-cased types and the chemically-bonded brick have proved an acceptable means of prolonging side wall life, even though there is still plenty of opportunity for improvement. With the operators of the larger furnace units, the trend is definitely to supplant silica side walls with basic brick.

21. The use of metal-cased and improved types of basic brick has been extended to door arches and jambs. They have been most successful in the pouring and slagging doors, where slag and flux attack is pronounced. Otherwise it would appear that they have not proved to hold any great advantage in this service.

22. The improved types of magnesite refractories have less spalling tendency, greater volume stability and somewhat greater load capacities at high temperatures than they had in their earlier forms. It is still necessary to observe precautions in heating and cooling these materials. The fact that basic brick will absorb large amounts of iron oxides, with ultimate failure due to structural changes, does not appear to be a deciding feature of their value in direct-arc furnaces, since their ultimate life is relatively short in any event.

Mortar

23. Basic brick are generally laid dry, expansion being considered sufficient to close the face of the brickwork and provide tight joints. While the writer must admit he has not seen a sufficient number of comparative situations to be firmly decided in this matter, there would appear to be merit in the use of a proper mortaring material. Properly used, it should help to equalize bearings and act as a seal in joints caused by uneven surfaces. Where used, the mortars are usually finely ground, dead-burned magnesite or chrome ore. With the former, oil is used in place of water to attain the necessary plasticity, as steam will cause dead-burned magnesite to slake quite readily. A minimum of water is used with chrome. Metal-cased products are always laid dry, although one of the above materials is used to fill in the spaces between the round type of metal-cased products.

CHROME REFRACTORIES

24. The use of chrome refractory products in direct-arc furnaces has been somewhat restricted because of the danger of contamination of the melt with chromium. Chrome refractories are subject to reduction under conditions prevailing in electric furnaces. This would offer no concern in units used to make chromium alloys. Three 10-ton units with chrome brick side walls and fire clay roofs, producing chrome steels, have been reported[1]. The writer knows of one small unit lined with chrome refractory, in which both chrome and chrome-free heats are made. When the latter are made, the side walls are banked higher. The practice has been successful.

25. This fear of chromium contamination has no doubt been a factor in preventing wider application of the chrome-magnesite

[1] Chivers, J. H., BULLETIN, American Ceramic Society, vol. 19, no. 11, Nov. 1940.

refractories, which appear to have generally better physical characteristics than the straight magnesite or chrome brick for use in direct-arc furnaces. However, their use for this service is increasing, and generally with satisfaction. They have comparatively good hot and cold strength, low shrinkage, high refractoriness and somewhat improved spall resistance. They are found in side walls, door arches and jambs. Their resistance to slag attack is excellent.

26. Chrome refractories are produced as burned and unburned, chemical-bonded, brick. Unlike magnesite, they are quite resistant to the attack of acid, as well as basic, slags. Their expansion, while greater than that of silica, is approximately two-thirds that of magnesite; their thermal conductivity is a little below that of magnesite. The unburned brick have improved spall resistance and greater mechanical strength.

27. It is not to be expected that the present types of chrome brick will find such wide application in electric furnaces as will the chrome-magnesite types, which are proving more satisfactory as their development progresses. The past few years have seen a marked increase in the use of the chrome-magnesite type for side wall construction in basic operations, and no doubt this trend will continue. Unfortunately, their development has not reached a stage where they are applicable to roofs.

SILLIMANITE REFRACTORIES

28. Sillimanite or mullite refractories have been used in direct-arc furnaces with good results, particularly in the roof, door arches and jambs. These refractories have a P.C.E. approximately 200°F. above that of silica, with spalling resistance generally better than that of super-duty fireclay brick. They have excellent hot load characteristics, equal to the best grades of silica brick. Thermal expansion is sufficiently low so that many users have found it unnecessary to make provision for it in roofs or door arches and jambs. Sillimanite refractories have somewhat better resistance to slags, particularly those high in lime, alkalies and fluorides, than do most alumina-silica refractories. The great fluxing power of FeO, toward the system Al_2O_3-SiO_2, can not be disregarded, and it apparently has not been determined just what the limiting conditions are. This unfavorable quality seems to be most apparent when furnaces are used to produce low-carbon steel for castings. More failures have been noted under this condition than

any other. But even so, the use of sillimanite roofs, door arches and jambs find favor in a few such operations.

29. It is in the alloy and tool steel industry that sillimanite has found the greatest favor. Roofs of sillimanite are being regularly used in direct-arc furnaces up to, and including, some of 50-tons capacity. The high temperatures and relatively long holding periods in such practice have often proved somewhat too severe for silica. The use of sillimanite refractories has proved satisfactory under these conditions because they possess a higher fusion point than silica brick with comparable load bearing and excellent spall resistance. Their greater initial cost has undoubtedly been a factor in preventing their more widespread use. However, the cost factor probably will become of less importance when operators become conscious of the longer life and decreased maintenance possible with such refractories.

Thermal Shock

30. Intermittent operation is not often a factor under present-day operating conditions; but where it is, or has been, sillimanite has proved a gratifying answer, by reason of its high resistance to thermal shock. It was for this reason that sillimanite found its first direct-arc furnace applications.

31. Door arch and jamb life have been a source of annoyance and lost production for many operators. Arches particularly are subject to thermal shock and to mechanical abuse in machine or hand-charged units. Sillimanite is an excellent refractory for this service, its complete success being limited only when conditions prevail that result in excessive attack by basic slags and fluxes. The use of sillimanite refractories in conjunction with basic side walls has, in some cases, balanced out the life of the door arches with that of the side walls. In other instances, while not equaling side wall life, they have proved economical by reducing lost time and frequency of arch repairs. Where spalling is severe, sillimanite is found to be superior to silica, basic or neutral refractories for this service. When so used in acid operations, the success has been more limited than with basic practice.

32. Jambs, in addition to thermal shock and mechanical abuse, are frequently subjected to slag attack. Sillimanite, while proving satisfactory in many cases, must be applied judiciously for this service by reason of the fluxing action of the slags.

Water Cooling

33. It would be well at this point to note the current tendency to use water-cooled arches and jambs. In some cases, no refractory is used at all, a metal water jacket in the form of an inverted "U" performing the functions of both the arch and jambs. Other operators use refractory jambs with a metal water jacket as the arch

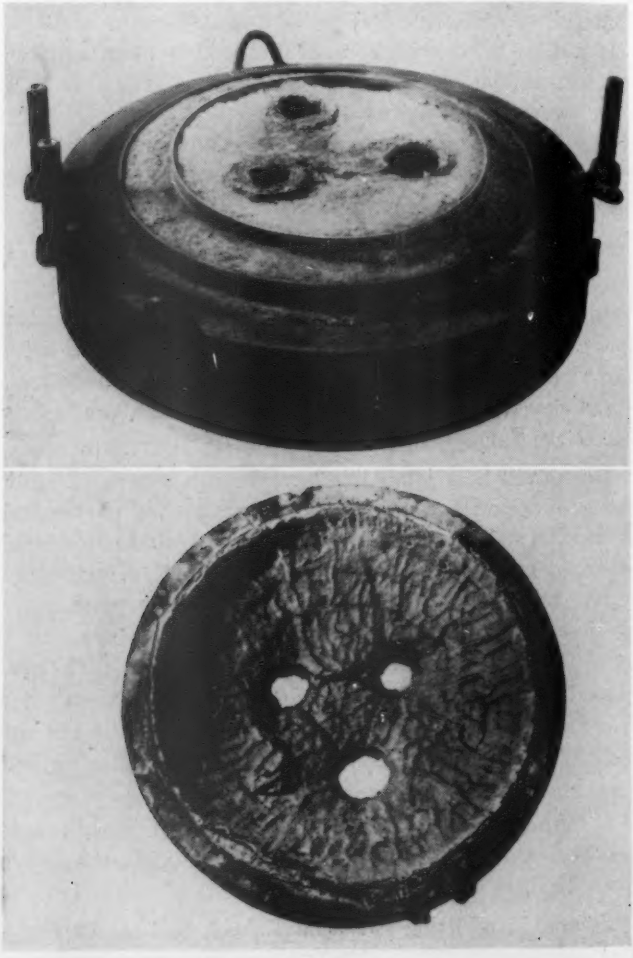

FIG. 3—TOP AND UNDERSIDE OF RAMMED SILLIMANITE ROOF ON SMALL DIRECT-ARC ELECTRIC FURNACE AFTER MELTING OVER 2000 HEATS OF NICKEL STEEL.

Still others follow these procedures but face the water jacket with silica or sillimanite. All of these methods appear to be proving effective. In the absence of a refractory material combining the best qualities of chrome-magnesite and sillimanite, there seems but little doubt that the trend will be to overcome the door arch, and quite likely the jamb problem by one or more of these methods. This will be particularly true of the operators of the larger electric furnace units.

34. Where sillimanite brick or shapes are used for roof, arch or jamb construction, it has been found that the use of a mortar of similar composition and fusion point will enhance the ultimate life of the refractory. It will minimize joint erosion, the danger of displacement of individual brick or shapes and also the effects of any thermal shock.

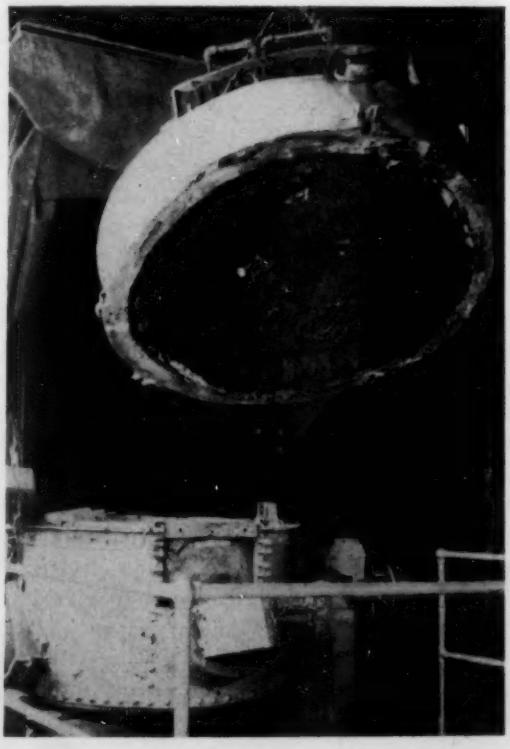

FIG. 4—RAMMED SILLIMANITE ROOF ON 1000-LB. DIRECT-ARC ELECTRIC FURNACE PRODUCING CHROM-NICKEL STEEL FOR CASTINGS AFTER 1650 HEATS.

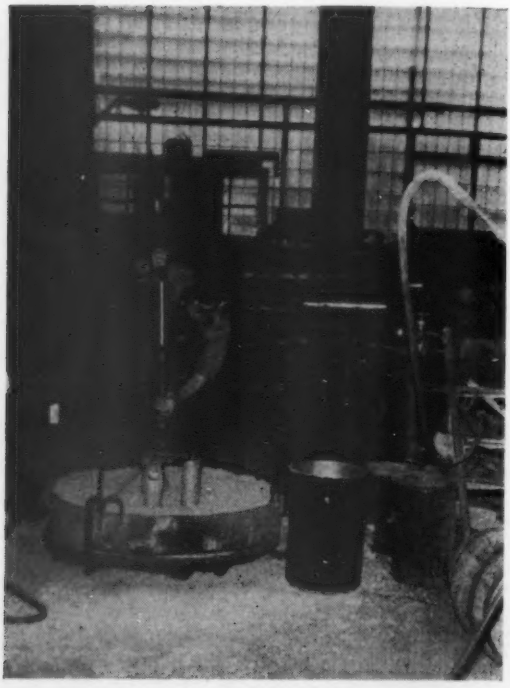

FIG. 5—RAMMING ROOF FOR SMALL DIRECT-ARC ELECTRIC FURNACE.

Rammed Linings

35. Rammed sillimanite linings have been used by some operators of direct-arc furnaces to form roofs for units of 1000 lb. per hour capacity and less (Figs. 3 and 4). Some operators of units of 500 lb. per hour capacity and less, have used it to form the side walls. When properly installed, such linings approximate the characteristics of the pre-burned brick and shapes. Other things being equal, the most important consideration in obtaining satisfactory service from this material is the actual ramming of the material into place (Fig. 5). If pneumatic or electric tools are not available for this operation, it is best not to give it further consideration, for it is questionable that a proper job can be accomplished by hand methods.

36. The proper preparation of monolithic linings of the sillimanite type can generally be achieved only by the manufacturer, since grain sizes and their proper proportioning are most important. Likewise, the moisture content is quite important; it should

not exceed 5 per cent by weight. Hand mixing of a dry aggregate with this amount of water is more difficult than is generally appreciated, if uniform distribution of the water is to be obtained. Unless mechanical mixing equipment is available, the ramming mix should be purchased from the manufacturer with the water already added.

37. In ramming, a layer not to exceed 2-in. deep is spread loosely in the form and rammed into place. The ramming should proceed until the mass is of uniform maximum density, which is indicated by the degree of plastic deformation showing adjacent to the point where the ramming tool is striking. The surface thus prepared should be thoroughly scarfed to a depth of about ¼-in. before the next layer is added. This is repeated until the requisite thickness is built up. The scarfing is necessary to obtain a good union between layers and to prevent the development of layer cracks when the unit is placed in service.

38. It is essential that the form used be solid and free from vibration. If the proper compaction is to be obtained, the ramming mix must be reasonably well contained within the form. This is readily done in ramming side walls but is more difficult in ramming a roof. In ramming a roof, the form, having the bottom side contour of the roof, is either constructed from wood and bolted to a concrete floor, or is sometimes formed of metal or as a concrete block. The roof ring is placed in position about the form and fastened rigidly in place. Ramming proceeds in a horizontal plane, one or two auxiliary roof rings being used, as the ramming progresses, to maintain the minimum thickness required in the roof as the bottom contour rises. These rings may be braced in place or held there by pins that are pushed down in the rammed body. Mandrels are used to form the electrode openings (Fig. 5). They are positioned by pins in the form, and must be held down while the ramming proceeds to prevent their working up. The mandrels are withdrawn when the ramming is completed. The auxiliary ring, or rings, may be left on the roof or may be split for ready removal when the ramming is completed. The side wall or roof made of rammed, sillimanite type, refractories should be dried by a wood or coke fire for about 48 hours before placing in service. When first placed in service, they should be heated slowly, but, after the first heat, they may be heated and cooled with less precaution than would be necessary with silica. Such structures will have heat and spall resisting qualities superior to silica and

will present a smooth surface, resistant to the attack of slags and fluxes, excepting those high in FeO.

SUPER-DUTY AND HIGH ALUMINA REFRACTORIES

39. A few operators use either super-duty fireclay brick or high alumina brick in direct-arc furnaces. They have been used in all positions above the bath.

40. Super-duty brick offer the advantages of being quite highly resistant to thermal shock, having a low porosity and fair hot strength, as well as a somewhat higher fusion point than silica brick. Their slag resistance is better than regular fire brick but is probably not as good as silica in direct-arc furnaces. Super-duty brick have not been used extensively and there appears to be no tendency on the part of operators to extend their use. Their principal advantage lies in their superior thermal shock resistance.

41. High alumina brick have a fusion point varying with their alumina content, ranging from around 3200° to about 3400°F. Their spall resistance is not equal to either super-duty fireclay brick or sillimanite. In addition, they have comparatively high porosity and generally an abnormally high shrinkage under conditions of continued high temperature service. They offer no particular advantage against slag attack. Their principal value would lie in their refractoriness which varies with the alumina content.

IMPROVING REFRACTORY SERVICE

42. Under present-day operating conditions, there is a marked tendency to push the production of furnace units far beyond anything the manufacturer originally intended. It is the refractories that take the brunt of such practice. The level of the metal bath is raised, with resultant greater intensity of heat radiation on the roof and side wall refractories. The action of slag, dusts and gases is pronounced. At the same time, higher voltages often are used, or the unit is held on high tap longer than would be normal. There is no good reason for this, as at lower voltages and higher amperages the same number of electrical units are available for the melting. The use of excessive voltages means longer arcs and greater radiation with less heat direct to the metal; which results in increased refractory costs.

43. Some of the bad effects resulting from furnace overcharging can be alleviated by increasing the distance between the bath and the roof. This is accomplished by the addition of two or three

courses of brick to the side walls. Where excessive voltages add to the difficulties, the remedy is obvious. Some of the abuse given refractories by operational practice can be avoided through care on the part of the operators, but little can be done today to improve the conditions resulting from the poor grades of scrap that are available.

44. With production the prime objective today, it is probable that the refractory manufacturers will find the operators demanding that their principal role be the supplying of better materials rather than attempting too much education in the use of those refractories now in service. Certainly they have been working toward improving their products and developing others. The operator must realize that anything can be wasted through lack of care. This applies to refractories as well as machines. The prime function of refractories is to resist the action of heat, fluxes and hot gases, and insufficient thought has been given to helping them along with their job. Of course, for any specific duty, there are destructive factors that can not be eliminated. However, some of these factors can be minimized without hindering production more than it would be hindered by lost operating time resulting from refractory repairs.

Care of Refractories

45. The stress of the times makes proper care and use of refractories more difficult than under normal conditions. The producer of electric steel today has to contend with low grade scrap and the inherent difficulties it involves. Excessive voltages have been mentioned, and conversations with men who are in a position to know would indicate that this is one abuse that is not generally justifiable. While wide variations exist in slags, the melter often can control them to a degree that will result in improved refractory service. Thin, watery slags generally are detrimental to refractories. If possible, they should be of a character to hold a maximum of heat in the bath so that their radiating qualities are minimized[1].

46. Care in charging can be observed, with good dividends in the form of longer refractory life. This applies not only to charging steel, but also to such additions as lime and iron ore. These latter should be added in a manner to reduce dusting and the resultant attack on refractory faces.

[1] Ibid.

47. The splattering of slag on the surface of the refractory faces is another abuse that can be minimized in most operations.

48. Each operator can, by careful observations, reduce the hazards the refractories must undergo. These observations will give him a better idea as to what constitute the most destructive factors acting on the refractories he uses. By giving the refractory manufacturer's engineers opportunity to check these observations with him, and fully discussing the problems involved, it is often possible that some suggestion can be made, or some different refractory offered to improve the service. It is such interchange of ideas and experience that will result in the improvement of refractories now in use and the development of new refractory products that may greatly improve operating conditions.

49. Laboratories of manufacturers of refractories are striving toward better products for many varied applications. Some of these products must have modified physical characteristics, others require chemical considerations and still others involve both. The problems of refractories for electric furnace applications are further complicated by the fact that the principal materials now recognized as available for such service are present as components in metallurgical slags.

DISCUSSION

Presiding: C. E. BALES, Ironton Fire Brick Co., Ironton, Ohio.

Co-Chairman: L. C. HEWITT, LaClede Christy Clay Products Co., St. Louis, Mo.

JOHN MCBROOM[1]: I would like to add a word to the comments that were so well presented in this paper. There are some refractory troubles, often laid to the refractory supplier, that are from causes beyond his control. Such are some of the furnace wall and roof refractory troubles that were mentioned. There is one point mentioned in this paper that I would like to emphasize, and that is the matter of electric furnace voltages and power characteristics.

In electric furnace operation, power consumed in the furnace is divided roughly into two parts. The first part is the power consumed in the arc. This power takes on the form of radiant heat which during the melt down period helps a great deal in melting the scrap around the arc. This same heat will melt furnace walls and roof when the scrap is all melted down. The second part of the power is consumed in forcing the current of the electrodes through the resistance of the metal charge which may be solid or molten charge. This is the power

[1] Hydro-Arc Furnace Corp., LaGrange, Ill.

that heats the bath, but this power does not do any damage to refractories unless the bath is overheated or held at high temperatures too long. This emphasizes the point that proper use of the different taps on a multi-voltage transformer will greatly increase refractory life.

The actual heating of metal bath, contrary to some thoughts that have been advanced on it, is due primarily to the current that is passed through it and is not due to the heat dissipated in the arc. Changing to a higher tap (higher voltage) toward the end of the heat, because it is not possible to heat up on the lower tap (lower voltage), may be necessary because of the fact that the full capacity of the transformer is not available on the lower tap, or it may be because, on the higher tap, it is possible to pull a high current without pulling voltage down to where electrodes dip and give carbon pick-up.

Once the charge is molten, the voltage should be adjusted to give the shortest possible arc length and yet keep the electrodes out of the bath. The arc should just be visible. If the arc is strung out for 1, 1½ or 2-in. on a molten bath for any period of time, trouble with refractories will be experienced.

When the furnace load current is decreased on a given tap, the heating effect on the metal is decreased, but the arc length is increased, damaging walls and roof. This results in a heavy slag formation. Electric furnace operators know how often they run into a thick, heavy slag when a heat must be held in the furnace after it is ready to tap. That is where the extra low tap on the transformer is very helpful. On most 4-position tap changing transformers, only the first 3 positions are needed for normal acid operation, but if the fourth position is properly connected and used when a heat must be held for a few minutes, the fourth position will permit reducing the load without increasing the arc length, and thus greatly increase the refractory life.

CHAIRMAN BALES: How much metal should be put in a 1-ton electric furnace? Would 3 tons be too much, or not?

MR. MCBROOM: That is a very interesting question and one that has caused a lot of trouble in the industry. A furnace rated at 1-ton per hour capacity usually will not hold over 2 tons of metal without risk of trouble. Three tons is definitely too much.

The amount of steel scrap that can be placed in a shell depends upon the density of the scrap. The maximum amount of metal that should go into a furnace shell depends upon the diameter of the shell. I believe that there is a definite ratio of depth of bath to diameter of bath that should not be exceeded for the most efficient operation. Just what that ratio is, I do not know. I have arbitrarily, so far, used a ratio of approximately 1 to 5 or 6. That is, a bath that is 1 ft. deep should not be over 5 or 6 ft. in diameter. I am not recommending that entirely, but that ratio has worked very well in some cases.

If a shell is too large and does not have enough metal on it, the bath is very thin and spread out over a wide area. This involves some problems in refining and in maintaining bottom refractories that are rather troublesome. On the other hand, if the bath is too deep for the diameter

of the bath, proper refinement is not obtained and it is difficult to heat material on bottom of dead bath. Now, of course, a good boil produces quite a bit of agitation, and will partially refine and heat a deep bath, but I have never felt that the full bath was properly refined if the 1 to 5 ratio of diameter to depth was exceeded.

The idea of rating the furnace size on a basis of hourly melting capacity is unsound. The hourly melting capacity is dependent upon the size of the transformer, demand limitations, melting practice, a large personnel factor, and many other variables. The diameter of a furnace shell determines the maximum size of the bath that should be put in that furnace shell, and I believe that a furnace shell should be rated accordingly. If that is done, we will get away from this business of bringing the bath way up on the side walls.

A furnace usually referred to as 1 ton-per-hour will have a shell which normally holds a 2-ton bath. In my mind, that is not a 1-ton shell—it is a 2-ton shell. This method of rating furnace sizes on maximum recommended size of charge of bath is used almost universally among European furnace builders and should be adopted in this country.

E. K. PRYOR *(author's closure):* I should like to express my appreciation for Mr. McBroom's remarks relative to electric furnace voltages and power characteristics. These remarks are certainly in order and quite worthy of the attention of every furnace operator.

As has been pointed out, when excessively long arcs are used, a good portion of the radiant heat is absorbed in the furnace walls and roof. Generally, in the operation of a direct-arc electric furnace, the temperatures are such that any slight increase in the amount of energy absorbed by the walls and roof will result in unusually rapid deterioration of the refractories.

A great many operators apparently are of the opinion that a few min. of such excessive heat will make no marked difference in the ultimate life of their refractories. This is not true. Raising the temperature of the refractory faces only 50 or 60°F. for a period of 10 or 15 min. frequently will result in more damage to the refractory than would be brought about by several days operation at the few degrees lower temperature. I have personally observed the complete destruction of a refractory lining in less than four shifts, due to slightly excessive temperatures, where normally the lining life would have been a matter of several hundred hours.

For any one refractory and any one operating condition there usually is a critical temperature for the refractory material. Higher temperatures will result in a several hundred-fold increase in the rate of destruction of the refractory.

A Study of Quenched Gray Cast Irons—Microstructure and Hardness†

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Translated and Condensed by F. R. Morral, State College, Pa.

Introduction

1. The hardening of steel has been studied extensively. The following factors have been found to affect it: (1) austenite composition, (2) amount, nature and distribution of insoluble particles in the austenite, (3) austenite grain size at the time of quenching, (4) size of specimen, and (5) the heat abstracting power of the quench. Considering this, a preliminary study has been made on the hardening of gray cast iron.

2. A gray cast iron to be of quality must have a pearlitic matrix, which, on being heated above the critical temperature of the material, will transform into austenite. There is a difference in the austenite composition of a gray cast iron from the austenite in steel. The solubility of carbon in the austenite iron is shifted by the silicon present[1].

3. A large amount of the carbon present in the cast iron is not combined, and that which is not is present in the form of graphite flakes, which may be expected to influence greatly the hardening of the iron.

4. Several methods are available to estimate the grain size of steels. There is a straight line relationship between the appearance of the fracture and grain size. A popular test to determine austenitic grain size is the McQuaid-Ehn test. Others are also available, but they are all unsuitable for determining the grain size of cast iron. It has been established, though, that the transformation of austenite to martensite, pearlite, ferrite, etc., does not bring about a change in grain size. Therefore, by microscopic examination, by observing the different orientations of the pearlite an indication is obtained of the size of the original austenite grain.

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† This paper is based upon a thesis submitted by A. Modolell to the Faculty of the Universidad Central de Madrid in partial fulfillment of the requirements for the Degree of Doctor of Science in 1941.

* Professor of Chemistry, University of Madrid, Spain, formerly at the University of Barcelona, and Graduate Student, University of Barcelona, respectively.

[1] Superior numbers refer to references at end of this paper.

Note: This paper was presented at a Gray Iron Session of the 47th Annual Meeting, American Foundrymen's Association, St. Louis, Mo., April 28, 1943.

5. Size of specimen and the heat abstracting power of the quench, the remaining two factors, were controlled in this investigation by maintaining the same conditions for the four irons investigated. All quenching experiments require a knowledge of the temperature to which the sample should be heated before quenching, soaking time and cooling rate. The first can be established if the critical temperatures are known, the time at temperature being long enough to permit the complete transformation of the pearlite into austenite. The cooling rate can be established indirectly by appropriate mechanical or metallographic tests.

6. If cast iron is solidified under proper conditions a pearlitic matrix, which is generally unstable, will form. The quality of the iron depends on this pearlitic matrix; therefore, in practice temperature and silicon are controlled because of their effect on this microconstituent. These two factors were studied[2] which made it possible to establish optimum conditions for use of a gray cast iron. This study also established the maximum desirable temperature to be used in the annealing of the iron to remove internal stresses with a minimum effect on the toughness.

7. The work on the changes occurring in the gray cast iron on water quenching from a high temperature is reported.

EXPERIMENTAL

8. The gray cast iron was made in a basic, industrial 3-phase heroult electric furnace. Four types of cast iron approximately of the 1, 1.5, 2 and 2.5 per cent silicon analysis were made by the addition of ferrosilicon (Table 1).

9. The molten metal at 2588°F. (1420°C.) was cast into rectangular molds 0.7 x 0.7 x 23.6-in. (18 x 18 x 600 millimeters).

Table 1

CHEMICAL ANALYSIS AND HARDNESS (AS CAST) OF FOUR CAST IRONS

Sample	Analysis, Per Cent					GC		Brinell Hardness	
	Si	Mn	S	P	TC	a end	z	a end	z
A	1.04					1.57	0.16	241	4'5
B	1.52					1.84	1.72	225	237
		0.54	0.045	0.134	2.86				
C	1.94					1.92	1.75	221	246
D	2.59					2.06	2.06	213	229

Samples were cut from these. The sample cut close to the gate was labelled *a*, the last one at the other end *z;* those in between were labelled, successively, *b, c, d,* etc. Only those portions of the bar were used which were similar in microstructure and showed little or no difference in hardness.

10. The heat treatment was carried out in an electric furnace with automatic temperature control. The temperatures were checked using a sensitive Leeds and Northrup potentiometer with a platinum-platinum-rhodium thermocouple.

11. Size of specimen and heat abstracting power of the quench have a decided effect on the hardening of steel. These two factors were not discussed in the introduction because the investigator tries to take care of them as best he can in his experimental procedure. All points of the specimen should have the right temperature, and all these points should be cooled at the same rate. Absorption of heat and loss of heat takes place by conduction through the mass of the body resulting in non-uniformity of heating and cooling. To obtain a certain degree of uniformity it is necessary to use very thin specimens and follow the same technique with all of them.

12. Dilatometric measurements were made with the Chevenard dilatometer[3] to determine the critical temperatures of the materials being investigated. Figure 1 shows some typical curves and Table 2 summarizes the data obtained.

13. The critical points known, the annealing temperatures were fixed at 1470°F. (800°C.), 1560°F. (850°C.) and 1650°F. (900°C.). In view of the great tendency of gray cast iron to graphitize, the time of soaking previous to quenching was chosen as ½-hour, one and 2 hours. Later, in view of the microscopic evidence

Table 2

CRITICAL POINTS OF GRAY CAST IRONS—IN °C.

Sample	*A*		*B*		*C*		*D*	
	A_c	A_r	A_c	A_r	A_c	A_r	A_c	A_r
	730	670	760	700	760	695	835	715
	745	690	770	690	775	705	800	710
	730	680	750	690	775	710	775	720
			750	865	790	700	800	710
	730	680						
	740	700					780	715
Average	735	684	767	693	775	703	798	714°C.
	1354	1260	1413	1279	1427	1297	1468	1317°F.

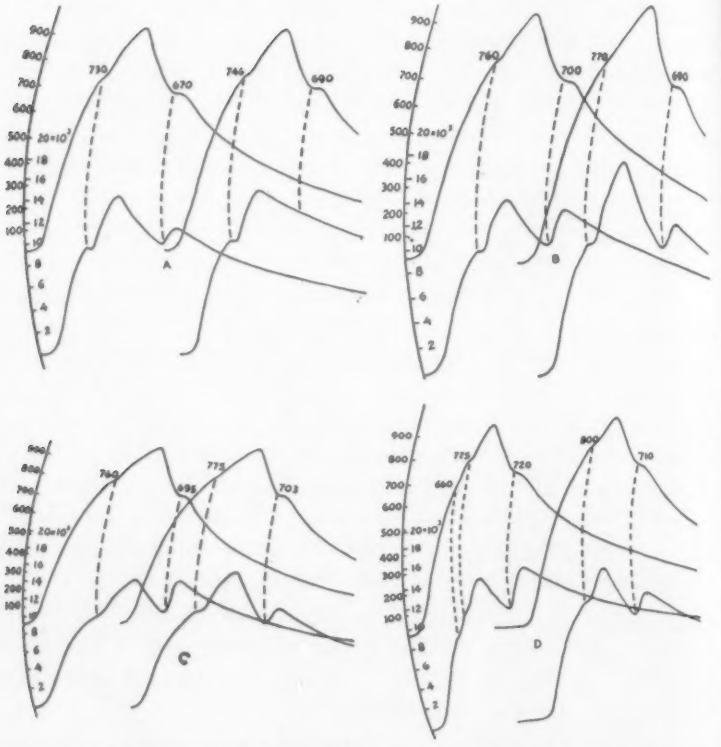

FIG. 1—TYPICAL DILATOMETER CURVES AS RECORDED ON CHEVENARD DILATOMETER FOR GRAY CAST IRONS "A, B, C AND D."

obtained, a new series of samples were run for 4 hours before quenching.

14. The technique used in this investigation was as follows: The samples (never more than four) were introduced into the furnace at temperature. The samples were spaced along the length of a loop of nichrome wire. After the appropriate soaking time they were removed by holding onto the nichrome wire knot with tongs and immediately immersed in water at 59 to 64°F. (15 to 18°C.). Care was taken to make certain that the samples attained the specified temperature, because when a transformation takes place there may be many degrees difference between the sample and the furnace.

15. The intensity and the efficiency of the quench was studied by means of hardness tests and by microscopic examination. Hard-

ness measurements were made on the samples after opposite surfaces had been brought down on the wheel. Flat, oxide-free surfaces were so prepared. The hardness of these thin samples was taken on an Alpha Durometer[4] (The Durometer test is similar to the Brinell test except that depth of penetration is reported. Thus the Durometer number decreases with increasing hardness). A difference in depth of 0.000079-in. (0.002 mm.) in the reading corresponds to a difference of one division on the Durometer.

16. The specimens were prepared for microscopic examination by a slightly modified technique of that recommended by Vilella[5]. The specimens were polished through No. 000 paper impregnated with soap solution. The finishing was accomplished on a satin covered disk rotating at a few r.p.m. with a heavy levigated alumina suspension in water. With this technique the outline of the graphite may be shown. Often the specimens were re-polished and re-etched to insure the examination of the true structure.

RESULTS AND DISCUSSION

17. The critical points of the four gray cast irons investigated with the dilatometer are listed in Table 2. The hardness data are summarized for the heat treatments for various time periods at temperature in Table 3. For brevity only average values of three readings are given.

18. Although the critical temperature data were just a means to an end, it is of interest to record a summary of the results:

1. Silicon raises the critical points.
2. The rise is greater for A_c points than for A_r points.
3. The differences are 58°F. (32°C.), 14°F. (8°C.) and 41°F. (23°C.) for the A_c points. For the A_r points the increases are: 16, 18 and 20°F. (9, 10 and 11°C.) for 1.54, 1.94 and 2.59 per cent silicon above the 1.04 per cent silicon.
4. Hysteresis increases with silicon composition, it being 92°F. (51°C.) for the *A* type cast iron, 133°F. (74°C.) for the *B*, 130°F. (72°C.) for the *C* and 151°F. (84°C.) for the *D* type gray cast iron.
5. On repeating the heating on the same sample, A_c was raised from 18 to 22°F. (10 to 12°C.). This was probably due to carbide decomposition.

19. Again for brevity the results of the hardness data (Table 3 and Figs. 2, 3 and 4) will only be summarized.

1. The effect of quenching is the more pronounced the longer the specimen is held at temperature. Microscopic examination will determine whether this is due to greater homogenization.
2. 1470°F. (800°C.) is high enough to harden iron *A*, *B* and *C* if they are held at temperature at least one hour.
3. 1560°F. (850°C.) is the minimum temperature necessary to harden iron *D*. The other irons give practically maximum hardness at this temperature.
4. Increasing the soaking temperature before quenching to 1650°F. (900°C.) only accentuates the irregularities of the hardening treatment. Quench-crack formation is favored at this temperature.
5. Samples at 1470°F. (800°C.) for ½-hour show anomalies. This may mean that this temperature and this time are just border-line. Small changes in conditions cause large differences on the hardening of the cast iron.

Table 3

HARDNESS OF GRAY CAST IRON AFTER QUENCHING IN WATER

Gray Cast Iron	*Hardness Before Quenching*	*Hours at Temp.*	*Hardness After Quenching from Temperature†* *(800°C.) 1470°F.*	*(850°C.) 1560°F.*	*(900°C.) 1650°F.*	*Typical Micros. Fig. No.*
A	230 (80.8)	½	215,510,500 (83,49,50)	510(49)	500(50)	5
		1	515 (48)	470(53)	515(48)	18, 19
		2	515, 470 (48,53)	480(52)	500(50)	10
		4	500 (50)	510(49)	490(51)	22, 27, 28
B	220 (82.0)	½	212,415,240 (84,56,78)	500(50)	490(51)	6
		1	480 (52)	460(54)	500(50)	
		2	470, 450 (53,55)	480(52)	500(50)	11, 13, 16
		4	425 (56)	490(51)	480(52)	23, 29
C	218 (82.3)	½	185,360,190 (88,63,87)	480(52)	480(52)	7, 8
		1	360 (62)	480(52)	490(51)	
		2	360, 330 (62,66)	490(51)	480(52)	14, 17
		4	235 (79)	510(49)	480(52)	24
D	212	½	136,135,152 (108,110,102)	425(56)	470(53)	9
		1	135 (110)	390(58)	470(53)	
		2	150, 135 (103,110)	460(54)	430(55)	15, 20, 21
		4	143 (106)	430(55)	420(57)	30, 32

† Alpha Durometer hardness numbers actually measured are indicated in parenthesis. Brinell Hardness numbers estimated from Table in reference 4.

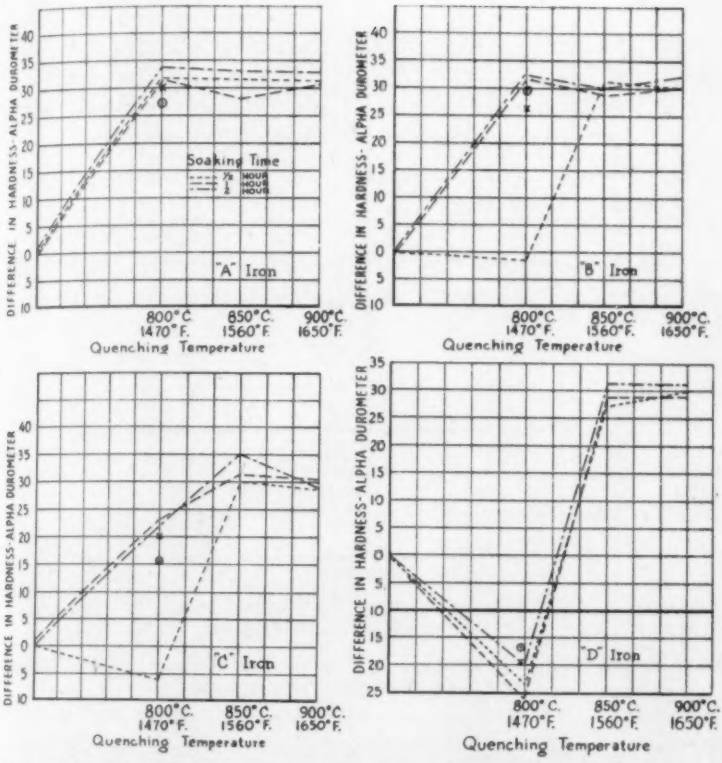

FIG. 2—GRAPHICAL REPRESENTATION OF THE EFFECTIVENESS OF QUENCHING GRAY CAST IRONS OF COMPOSITIONS "A, B, C AND D" FROM VARIOUS TEMPERATURES AFTER SOAKING FOR VARYING PERIODS OF TIME.

20. A gray cast iron cooled from the melt has a definite microstructure. Heating this iron above its critical temperature will destroy its cast structure. The cementite should change into austenite. Some of this austenite may decompose because of time and temperature. The remaining austenite on cooling below the critical will give a new pearlite distinct from that formed on casting.

21. Examination of the micrographs of sample held at 1470°F. (800°C.) for only ½-hour and quenched show the cast microstructure. See Figs. 5, 6, 7, 8 and 9. The effect of longer heating time and quenching is shown in Figs. 10 to 17 for samples held at 1470°F. Figures 18 to 21 show the structures obtained on quenching from temperatures higher than 1470°F. (800°C.).

22. The relationship between austenite grain size and hardenability has been studied for steels[6]. Murphy and Wood[7] investi-

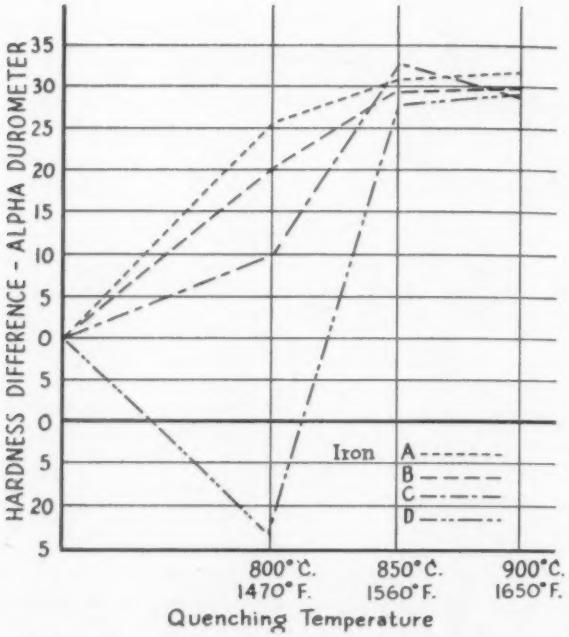

FIG. 3—AVERAGE CHANGE IN HARDNESS AT EACH QUENCHING TEMPERATURE IN RELATION TO SILICON COMPOSITION OF THE IRONS.

gated four cast irons of the same composition which possessed varying tensile strengths but had the same distribution and size of graphite flakes. The strengths found were in direct proportion to the grain size. They explained this by the greater hardenability of the large grain. The present work confirms this influence of grain size on the hardenability of cast iron. Grain growth, together with the time necessary to dissolve the carbide in the pearlite, explain the great difference in hardness found on quenching from 1470°F. (800°C.). It should be noted that the samples were held at this temperature ½, one, 2 and 4 hours.

23. The grain size of the austenite in the cast iron was deduced, as indicated in the introduction, from the appearance of the final product. If pearlite of the cast iron were decomposed into ferrite and graphite, the ferrite grain boundaries would correspond to those of the austenite.

24. Figures 12, 16, 17, 20 and 22 show that, when simultaneously with the austenite transformation, cementite precipitates out of the hypereutectoid matrix, or ferrite from the hypereutectoid, the

precipitate deposits in normal structures, the crystalline boundaries. The resulting acicular structure corresponds to the austenite grains. These micros indicate also the heterogeneity of composition. Due to the presence of graphite there can not be a perfect distribution of the carbide at the temperature. Certain ranges of carbon may be present in solid solution in the stable austenite depending on the silicon content[1].

25. Figures 18 and 19 actually show the presence of two different types of martensite in the same sample, the two regions being separated by a crack. The difference between the two martensites was so great that it caused great stresses in the matrix. Cracks may also be present without a decided difference in the structure. Large grained cast irons are usually subject to greater dimensional changes and residual stresses than fine grained material.

26. Figures 12 to 15 show various zones and their separation. Figures 11 to 14, 16, 17, 20 and 21 show some grains which were not etched by picric acid. This type of structure has not been found mentioned or reproduced in the extensive bibliography con-

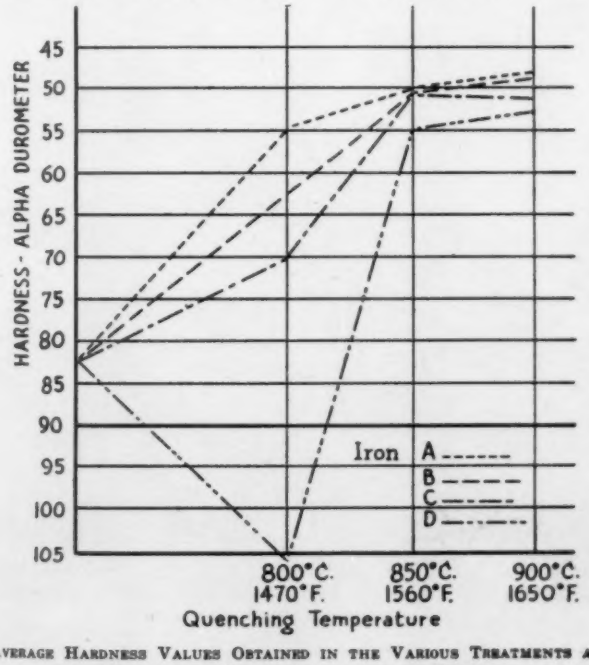

FIG. 4—AVERAGE HARDNESS VALUES OBTAINED IN THE VARIOUS TREATMENTS AND THEIR RELATION TO SILICON CONTENT OF THE IRONS.

sulted. An explanation was sought and the following seems plausible. While heating a cast iron at a certain temperature the pearlitic matrix may be transforming or ready to transform. The quenching means a sudden interruption of the transformation; or better, a paralyzation of certain phases of the transformation. In view of this it was considered necessary to obtain a better homogeneity of the cast iron at the temperature studied than was at first thought necessary. By this means the changes present during the transformation of the pearlite may be eliminated. Any effect observed will be due mainly to the effect of quenching. Precautions had to be taken to prevent excessive oxidation, and, therefore, the samples were held at temperature only for 4 hours.

27. The results in Table 3 indicate that irons *A* and *B* have hardened; *C* and *D* have not. This agrees with the critical temperature data previously obtained.

28. Figure 22 is uniformly martensitic for iron *A*. Figures 23

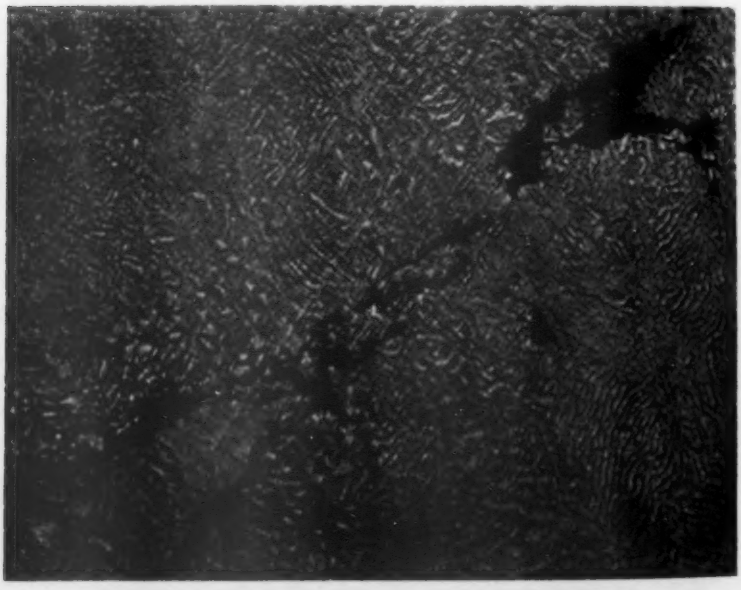

FIG. 5—PHOTOMICROGRAPH OF "A" IRON (SAMPLE A_{bs}), SILICON 1.04 PER CENT, SOAKED ½-HOUR AT 1470°F. (800°C.)—WATER QUENCHED AT 61°F. (16°C.). BHN* 215 (AHN** 83). ETCHED. MAGNIFICATION x1500.

* Brinell Hardness Number.
** Alpha Durometer Hardness Number.

FIG. 6—PHOTOMICROGRAPH OF "B" IRON (SAMPLE B_{52}). SILICON 1.52 PER CENT, SOAKED ½-HOUR AT 1470°F. (800°C.)—WATER QUENCHED AT 61°F. (16°C.). BHN 215 (AHN 83). ETCHED. MAGNIFICATION x1500.

FIG. 7—PHOTOMICROGRAPH OF "C" IRON (SAMPLE C_{42}). SILICON 1.94 PER CENT, SOAKED ½-HOUR AT 1470°F. (800°C.)—WATER QUENCHED AT 61°F. (16°C.). BHN 360 (AHN 63). ETCHED. MAGNIFICATION x1500.

FIG. 8—PHOTOMICROGRAPH OF "C" IRON (SAMPLE C_{12}), SILICON 1.94 PER CENT, SOAKED ½-HOUR AT 1470°F. (800°C.)—WATER QUENCHED AT 61°F. (16°C.). BHN 190 (AHN 87). ETCHED. MAGNIFICATION x1500.

FIG. 9—PHOTOMICROGRAPH OF "D" IRON (SAMPLE D_{12}), SILICON 2.59 PER CENT, SOAKED ½-HOUR AT 1470°F. (800°C.)—WATER QUENCHED AT 61°F. (16°C.). BHN 152 (AHN 102). ETCHED. MAGNIFICATION x1500.

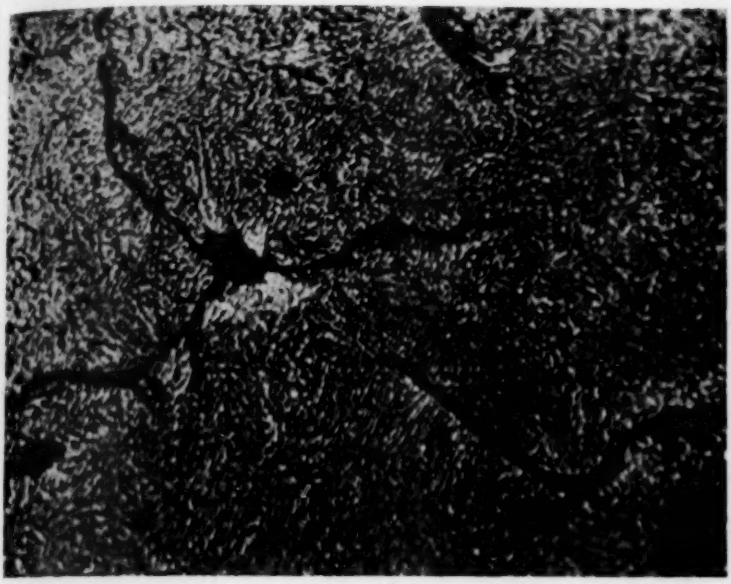

FIG. 10—PHOTOMICROGRAPH OF "A" IRON (SAMPLE A_{c2}), SILICON 1.04 PER CENT, SOAKED 2 HOURS AT 1470°F. (800°C.)—WATER QUENCHED AT 61°F. (16°C.). BHN 515 (AHN 48). ETCHED. MAGNIFICATION x1500.

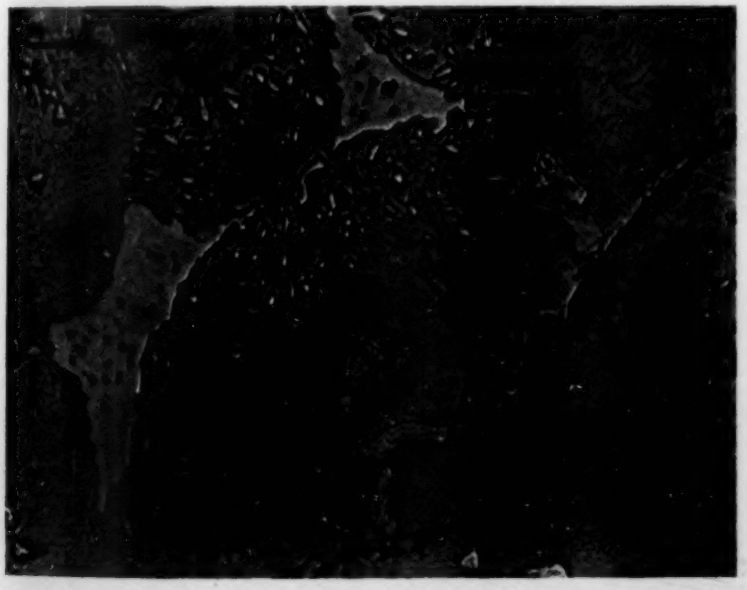

FIG. 11—PHOTOMICROGRAPH OF "B" IRON (SAMPLE B_{c2}), SILICON 1.52 PER CENT, SOAKED 2 HOURS AT 1470°F. (800°C.)—WATER QUENCHED AT 61°F. (16°C.). BHN 470 (AHN 53). ETCHED. MAGNIFICATION x1500.

FIG. 12—PHOTOMICROGRAPH OF "B" IRON (SAMPLE B_{c2}), SILICON 1.52 PER CENT, SOAKED 2 HOURS AT 1470°F. (800°C.)—WATER QUENCHED AT 61°F. (16°C.). BHN 470 (AHN 53). ETCHED. MAGNIFICATON x350.

FIG. 13—PHOTOMICROGRAPH OF "B" IRON (SAMPLE B_{c2}), SILICON 1.52 PER CENT, SOAKED 2 HOURS AT 1470°F. (800°C.)—WATER QUENCHED AT 61°F. (16°C.). BHN 470 (AHN 53). ETCHED. MAGNIFICATION x90.

FIG. 14—PHOTOMICROGRAPH OF "C" IRON (SAMPLE C_{c2}), SILICON 1.94 PER CENT, SOAKED 2 HOURS AT 1470°F. (800°C.)—WATER QUENCHED AT 61°F. (16°C.). BHN 360 (AHN 62). ETCHED. MAGNIFICATION x90.

FIG. 15—PHOTOMICROGRAPH OF "D" IRON (SAMPLE D_{c2}), SILICON 2.59 PER CENT, SOAKED 2 HOURS AT 1470°F. (800°C.)—WATER QUENCHED AT 61°F. (16°C.). BHN 150 (AHN 103). ETCHED. MAGNIFICATION x90.

FIG. 16—PHOTOMICROGRAPH OF "B" IRON (SAMPLE B_{12}), SILICON 1.52 PER CENT, SOAKED 2 HOURS AT 1470°F. (800°C.)—WATER QUENCHED AT 63.5°F. (17.5°C.). BHN 450 (AHN 55). ETCHED. MAGNIFICATION x1500.

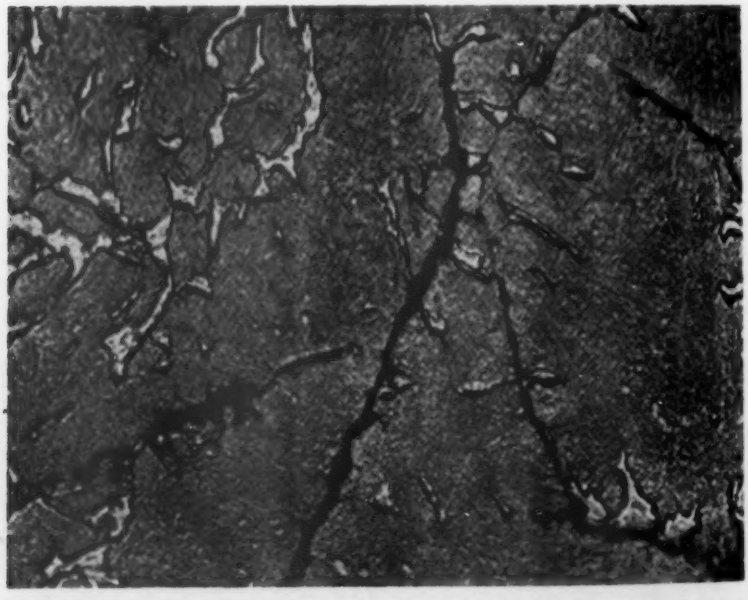

FIG. 17—PHOTOMICROGRAPH OF "C" IRON (SAMPLE C_{12}), SILICON 1.94 PER CENT, SOAKED 2 HOURS AT 1470°F. (800°C.)—WATER QUENCHED AT 63.5°F. (17.5°C.). BHN 330 (AHN 66). ETCHED. MAGNIFICATION x1500.

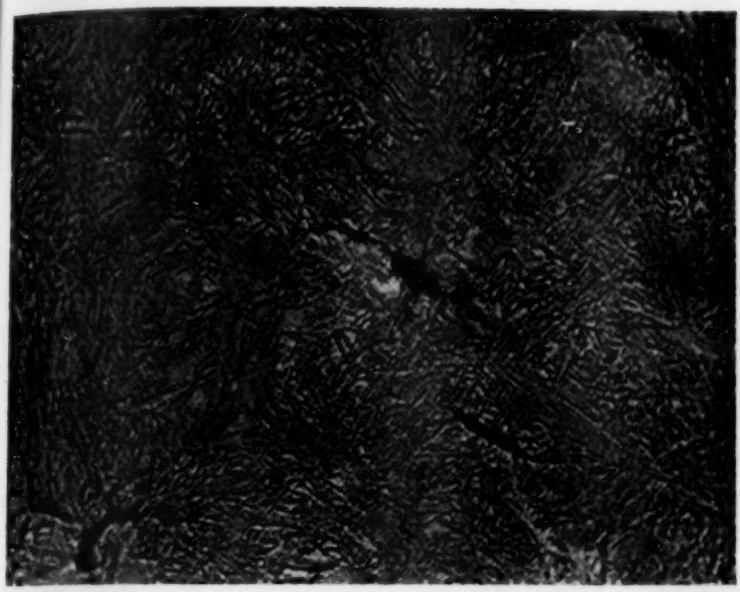

FIG. 18—PHOTOMIROGRAPH OF "A" IRON (SAMPLE A_{12}), SILICON 1.04 PER CENT, SOAKED 1 HOUR AT 1560°F. (850°C.)—WATER QUENCHED AT 59°F. (15°C.). BHN 470 (AHN 53). DIFFERENT MARTENSITES SEPARATED BY CRACK. ETCHED. MAGNIFICATION x1500.

FIG. 19—PHOTOMICROGRAPH OF "A" IRON (SAMPLE A_{12}), SILICON 1.04 PER CENT, SOAKED 1 HOUR AT 1560°F. (850°C.)—WATER QUENCHED AT 59°F. (15°C.). BHN 470 (AHN 53). DIFFERENT MARTENSITES SEPARATED BY CRACK. ETCHED. MAGNIFICATION x1500.

FIG. 20—PHOTOMICROGRAPH OF "D" IRON (SAMPLE D_{c2}), SILICON 2.59 PER CENT, SOAKED 2 HOURS AT 1560°F. (850°C.)—WATER QUENCHED AT 59°F. (15°C.). BHN 460 (AHN 54). ETCHED. MAGNIFICATION x1500.

FIG. 21—PHOTOMICROGRAPH OF "D" IRON (SAMPLE D_{k2}), SILICON 2.59 PER CENT, SOAKED 2 HOURS AT 1650°F. (900°C.)—WATER QUENCHED AT 63.5°F. (17.5°C.). BHN 430 (AHN 55). ETCHED. MAGNIFICATION x1500.

FIG. 22—PHOTOMICROGRAPH OF "A" IRON (SAMPLE A_{b3}), SILICON 1.04 PER CENT, SOAKED 4 HOURS AT 1470°F. (800°C.)—WATER QUENCHED AT 62.6°F. (17°C.). BHN 500 (AHN 50). ETCHED. MAGNIFICATION x1500.

FIG. 23—PHOTOMICROGRAPH OF "B" IRON (SAMPLE B_{b3}), SILICON 1.52 PER CENT, SOAKED 4 HOURS AT 1470°F. (800°C.)—WATER QUENCHED AT 62.6°F. (17°C.). BHN 415 (AHN 56). ETCHED. MAGNIFICATION x1500.

and 24, for irons *B* and *C*, show two constituents in the matrix. The same is true for iron *D*. One of the microconstituents is characteristic for martensite. The other is not attacked by picric acid. With the unattacked constituent present in larger amounts the hardness decreases, so it is logical to assume that it is ferrite.

29. Transformations in solid phases take place by three separate mechanisms, which may occur at different temperatures and with individual reaction rates. These mechanisms are: Lattice transformation, from face centered cubic to the tetragonal iron for martensite, or body centered cubic of regular alpha iron; atomic reorganization within the lattice and, finally, change in chemical composition. Generally, the chemical composition change is the last process and the slowest, while the change in lattice and the atomic rearrangement take place almost always simultaneously. The three processes coincide only, probably, when the transformation takes place at high temperatures.

30. Apparently on heating the specimen to 1470°F. (800°C.) the iron carbide in the pearlite has not dissolved completely in the ferrite of the pearlite. There remain, therefore, nuclei of ferrite which favor its growth on passing the eutectoid line. In other words, the change in composition has not been complete in the phase transformation, this being the slowest of the three processes, and the one which offers greatest resistance because of slow diffusion. Therefore, no atomic rearrangement can take place, nor a geometric transformation of the lattice.

31. At 1560°F. (850°C.) for 4 hours all four irons have hardened. At this higher temperature the solution of the carbide has taken place completely and the changes which may occur are those which are typical for austenite.

32. Iron *A* in spots shows the structure of martensite shown in Fig. 25, and in other spots the structure of Fig. 26. Figures 27 and 28 show the transition structure from the one to the other. The polyhedra in Fig. 29 are of austenitic character (note the softness of these regions; they are scratched, while the martensitic surfaces are well polished). Figure 27 shows the transformation of austenite into martensite. The transformation speed of the austenite in this cast iron at the given temperature is so low that a certain amount of austenite is retained. In Fig. 28 the interlacing of martensitic needles and austenite polyhedra may be observed.

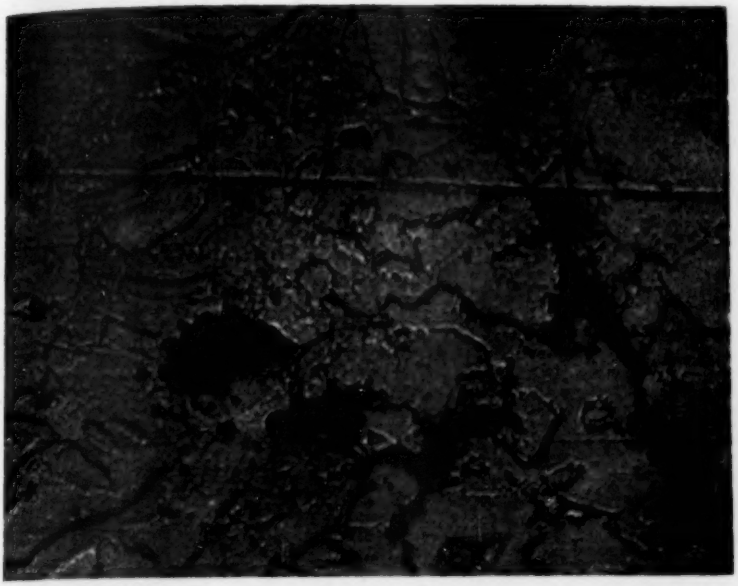

FIG. 24—PHOTOMICROGRAPH OF "C" IRON (SAMPLE C_{b3}), SILICON 1.94 PER CENT, SOAKED 4 HOURS AT 1470°F. (800°C.)—WATER QUENCHED AT 62.6°F. (17°C.). BHN 240 (AHN 79). ETCHED. MAGNIFICATION x1500.

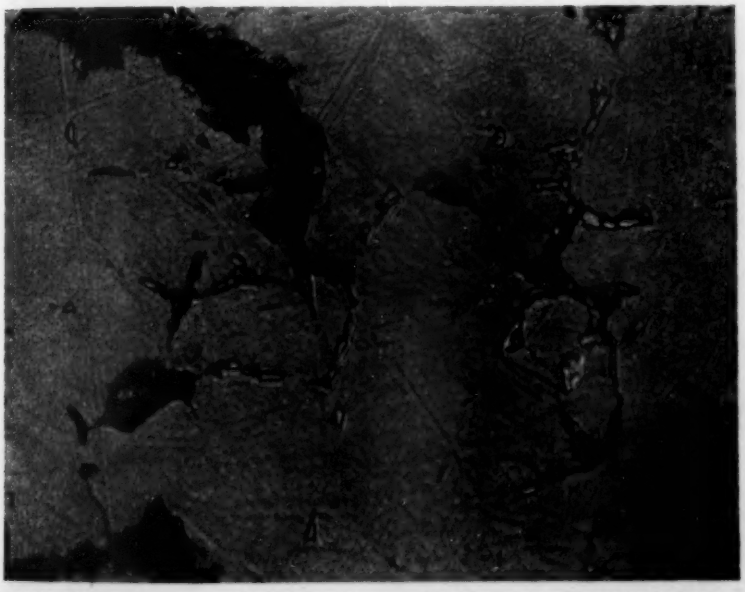

FIG. 25—PHOTOMICROGRAPH OF "A" IRON (SAMPLE A_{c3}), SILICON 1.04 PER CENT, SOAKED 4 HOURS AT 1560°F. (850°C.)—WATER QUENCHED AT 62.6°F. (17°C.). BHN 510 (AHN 49). NOTE DIFFERENT STRUCTURES APPEARING IN THIS SAMPLE (FIGS. 26, 27 AND 28). ETCHED. MAGNIFICATION x1500.

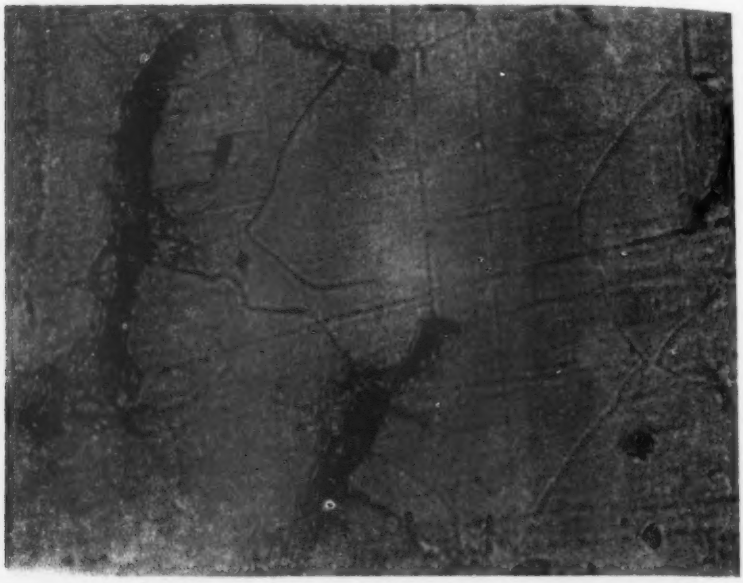

FIG. 26—PHOTOMICROGRAPH OF "A" IRON (SAMPLE A_{cs}), SILICON 1.04 PER CENT, SOAKED 4 HOURS AT 1560°F. (850°C.)—WATER QUENCHED AT 62.6°F. (17°C.). BHN 510 (AHN 49). ETCHED. MAGNIFICATION x1500.

FIG. 27—PHOTOMICROGRAPH OF "A" IRON (SAMPLE A_{cs}), SILICON 1.04 PER CENT, SOAKED 4 HOURS AT 1560°F. (850°C.)—WATER QUENCHED AT 62.6°F. (17°C.). BHN 510 (AHN 49). ETCHED. MAGNIFICATION x1500.

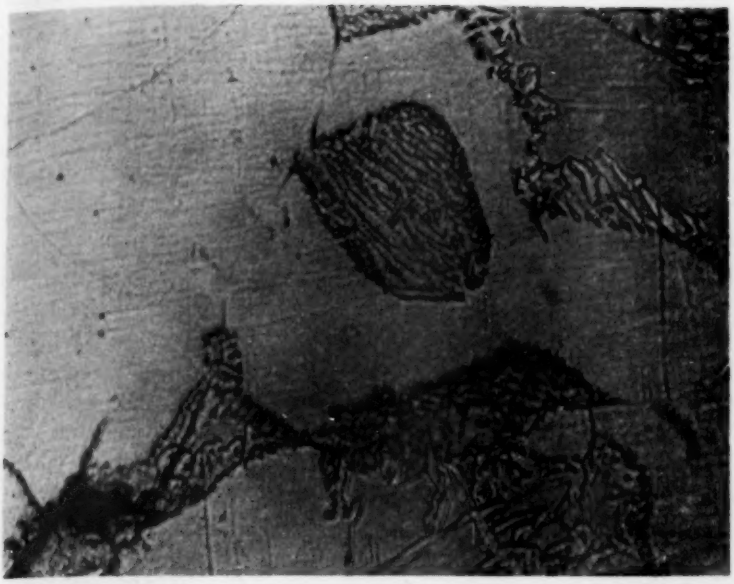

FIG. 28—PHOTOMICROGRAPH OF "A" IRON (SAMPLE A_{cs}), SILICON 1.04 PER CENT, SOAKED 4 HOURS AT 1560°F. (850°C.)—WATER QUENCHED AT 62.6°F. (17°C.). BHN 510 (AHN 49). ETCHED. MAGNIFICATION x1500.

FIG. 29—PHOTOMICROGRAPH OF "B" IRON (SAMPLE B_{cs}), SILICON 1.52 PER CENT, SOAKED 4 HOURS AT 1560°F. (850°C.)—WATER QUENCHED AT 62.6°F. (17°C.). BHN 490 (AHN 51). ETCHED. MAGNIFICATION x1500.

33. Irons *B* and *C* give only martensite, but the austenite polyhedra were seen for iron *D*. See Figs. 30 to 32.

34. The irons held at 1650°F. (900°C.) for 4 hours and quenched, offered nothing in particular. They only showed some oxidation and presented a martensitic structure.

35. A series of experiments were performed to check on whether the crystals in Figs. 23 and 24 were ferrite and those of Figs. 25 to 29 and 31 were austenite. To insure that the structures observed were not only a superficial effect, such as oxidation or decarburization, the samples were re-polished at a surface 0.13-in. (4 mm.) below the one studied, taking all necessary precautions. Hardness measurements, which were the same as those already reported, were taken. With this check on previous work certain tempering experiments were followed. Table 4 summarizes the results. It gives the initial and the final hardness of the samples treated, the tempering treatment given and the number of the photomicrograph typical for the structure obtained. In the last column, "remarks," the type of structure is indicated.

36. In the *A* iron sample the intermediate structures had disappeared and some of the polyhedral surfaces had decreased notably. At 750°F. (400°C.) this sample had a completely martensitic structure. Only a few representative results are shown in this paper, characteristic of the structures examined. The ferritic nature of the other polyhedra is shown. They have grown in size on comparing Figs. 16 and 17 with 35 and 36.

37. The results of Schwartz and Barnett[8] have also been confirmed in the course of this investigation; namely, that the precipitation of carbon is greater at a temperature near the critical

Table 4

TEMPERING OF SAMPLES CONTAINING FERRITE AND AUSTENITE CRYSTALS

	Before Tempering			*After Tempering*		
Sample	*Hardness***	*Fig. No.*	*Heat Treatment °F.*	*Hardness***	*Fig. No.*	*Remarks*
A_{c3}	510(49)	25 to 28	½ hr at 660 (350°C.)	460(53)	33	Austenite Polyhedra
			½ hr at 750 (400°C.)	400(58)	34	Martensite
			½ hr at 930 (500°C.)*	285(71)		
B_{b3}	420(56)		½ hr at 750 (400°C.)	330(65)		Ferrite
B_{12}	430(55)	16	½ hr at 930 (500°C.)	265(74)	35	Ferrite
C_{12}	320(66)	17	½ hr at 930	210(86)	36	Ferrite

* Sample allowed to cool slowly in furnace.
** Hardness expressed in Brinell, numbers in parenthesis actually Alpha Durometer hardness numbers measured.

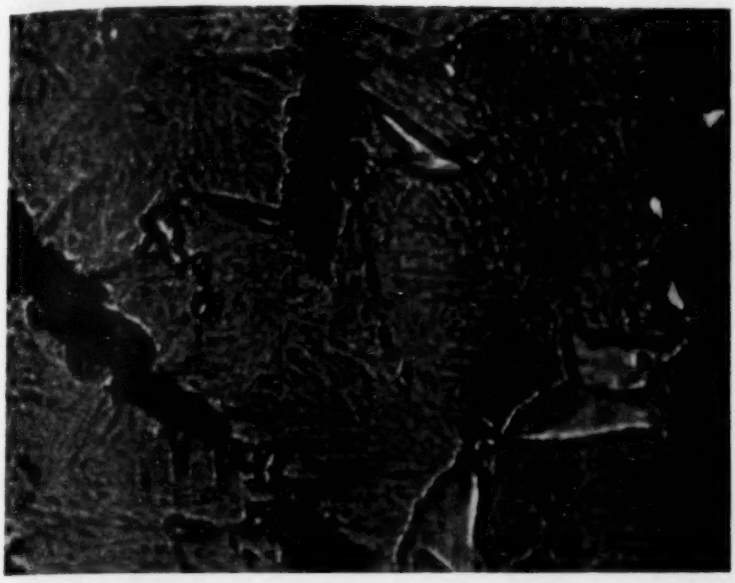

FIG. 30—PHOTOMICROGRAPH OF "D" IRON (SAMPLE D_{c2}), SILICON 2.59 PER CENT, SOAKED 4 HOURS AT 1560°F. (850°C.)—WATER QUENCHED AT 62.6°F. (17°C.). BHN 430 (AHN 55). ETCHED. MAGNIFICATION x1500.

FIG. 31—PHOTOMICROGRAPH OF "D" IRON (SAMPLE D_{c2}), SILICON 2.59 PER CENT, SOAKED 4 HOURS AT 1560°F. (850°C.)—WATER QUENCHED AT 62.6°F. (17°C.). BHN 430 (AHN 55). ETCHED. MAGNIFICATION x1500.

FIG. 32—PHOTOMICROGRAPH OF "D" IRON (SAMPLE D_{cs}), SILICON 2.59 PER CENT, SOAKED 4 HOURS AT 1560°F. (850°C.)—WATER QUENCHED AT 62.6°F. (17°C.). BHN 430 (AHN 55). ETCHED. MAGNIFICATION x500.

FIG. 33—PHOTOMICROGRAPH OF "A" IRON (SAMPLE A_{cs}), SILICON 1.04 PER CENT, SOAKED 4 HOURS AT 1560°F. (850°C.)—WATER QUENCHED AT 62.6°F. (17°C.). FURTHER TEMPERED ½-HOUR AT 660°F. (350°C.) AND WATER QUENCHED. ETCHED. MAGNIFICATION x1500.

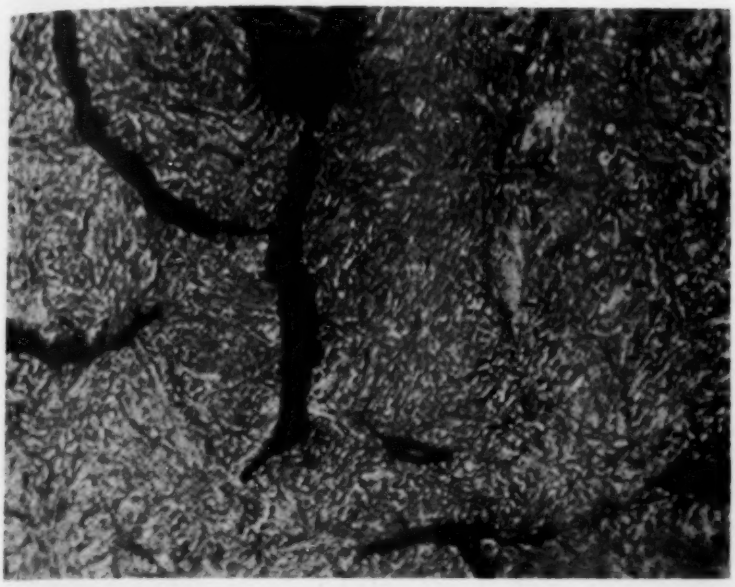

FIG. 34—SAME AS FIG. 33 WITH FURTHER TEMPERING AT 750°F. (400°C.) AND AIR COOLED.

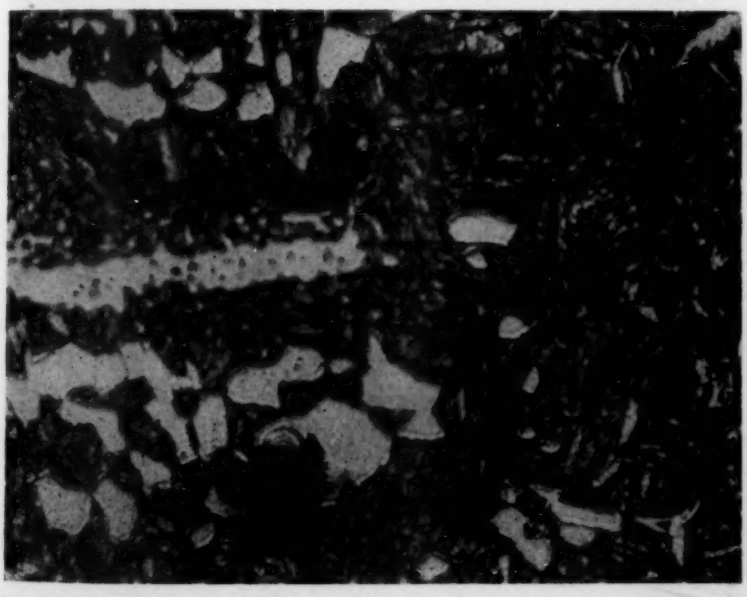

FIG. 35—PHOTOMICROGRAPHS OF "B" IRON (SAMPLE B_{c2}), SILICON 1.52 PER CENT, SOAKED 2 HOURS AT 1470°F. (800°C.)—WATER QUENCHED AT 60.8°F. (16°C.). FURTHER TEMPERED ½-HOUR AT 930°F. (500°C.) AND FURNACE COOLED. ETCHED. MAGNIFICATION x1500.

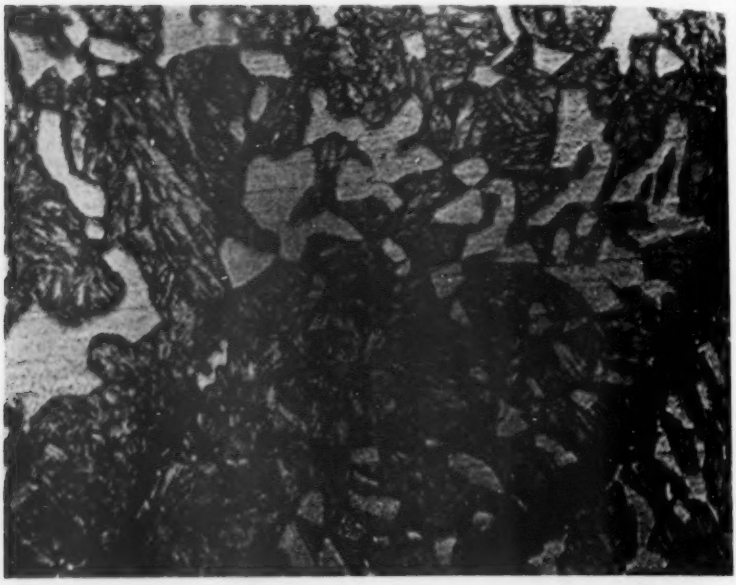

FIG. 36—PHOTOMICROGRAPH OF "C" IRON (SAMPLE C_{12}), SILICON 1.94 PER CENT, SOAKED 2 HOURS AT 1470°F. (800°C.)—WATER QUENCHED AT 63.5°F. (17.5°C.). FURTHER TEMPERED ½-HOUR AT 930°F. (500°C.) AND FURNACE COOLED. ETCHED. MAGNIFICATION x1500.

than in the temperature zones above or below it. In fact, there was no pearlite decomposition for the time period to which the cast irons were subjected either on heating or to tempering.

SUMMARY OF CONCLUSIONS

38. The following summarizes the conclusions that can be drawn from this investigation:

1. It has been determined which quenching treatment gives best results in the cast irons under investigation with the least likelihood of quench-crack formation.
2. Silicon raises critical points.
3. The effects of quenching are the more pronounced the longer the specimen is held at temperature:
   (a) 1470°F. (800°C.) is high enough for irons *A*, *B* and *C*, if they are held at temperature at least one hour.
   (b) Iron *D* requires a temperature near 1560°F. (850°C.).

4. The following results were obtained by microscopic examination:
   (a) Heating 2 hours at 1560°F. (850°C.) before quenching, the structure was completely martensitic.
   (b) Heating 4 hours at 1560°F. before quenching gives some austenitic polyhedra and martensite.
   (c) Heating at 1650°F. (900°C.) for 2 hours, some austenitic polyhedra were found.
   (d) Heating 4 hours at 1650°F. (900°C.) a completely martensitic structure was apparent.

39. The structure containing austenite produced by water quenching from 1560°F. is unusual in cast irons that apparently contain no elements that extend the gamma transformation range, since silicon rather restricts this field and the amount of manganese present is not enough to retain austenite. Lately, various investigators such as Bain, Allen, Portevin, Wever and Lange have studied the decomposition of austenite, upon cooling, as a function of time and temperature for ordinary steels. Consequently, we shall continue our investigation, as it seems necessary to determine precisely what retards this transformation in cast irons.

References

1. Timmins, A. A., *"Decomposition of Pearlite in Grey Cast Iron,"* JOURNAL, Iron & Steel Institute (London), vol. 142, no. 2, pp. 123p-140p (1940).
2. Modolell, A., Thesis, University of Barcelona, Spain.
3. Chevenard, P., REVIEW METALLURGIE, vol. 14, p. 610 (1917).
4. Williams, S. R., *"Hardness and Hardness Measurements,"* METALS HANDBOOK, American Society for Metals (1942).
5. Vilella, A., METALS AND ALLOYS, vol. 3, p. 205 (1932).
6. Bain, E. C., TRANSACTIONS, American Institute of Mining and Metallurgical Engineers, Iron & Steel Div., vol. 138, p. 57 (1938).
7. Murphy and Wood, TRANSACTIONS, American Society for Metals, vol. 24, p. 75 (1936).
8. Schwartz, H. A., and Barnett, M. K., *"The Velocity of Conversion of Austenite to Ferrite and Cementite,"* TRANSACTIONS, American Foundrymen's Association, vol. 49, pp. 441-448 (1941).

DISCUSSION

(In the absence of the authors this paper was presented by Dr. Wm. Pennington, Industrial Fellow, Mellon Institute, Pittsburgh, Penn.)

E. R. STARKWEATHER[1] *(written discussion):* The paper presents in excellent detail a study of the results of prolonged soaking time and rapid quenching rates. For this type of study, very small specimens are desirable. However, for practical results, the soaking time—4 hours in some instances—seems to be longer than really necessary. Likewise, it is surprising that more cracking did not occur on the water quench.

To my mind, the authors should be given all possible praise, as work of this nature should be encouraged.

G. A. TIMMONS[2] *(written discussion):* There is no doubt that the authors experienced considerable difficulty, but I feel they were more severely handicapped by lack of recent literature. Had they been familiar with the following references, they would have had considerably less difficulty in explaining some of the phenomena they observed:

(a) Greiner, Marsh & Stoughton, ALLOYS OF IRON AND SILICON, McGraw Hill (1933).

(b) Crosby, V. A., Herzig, A. J., and Timmons, G. A., *"Some Factors Involved in Hardening and Tempering Gray Cast Iron,"* TRANSACTIONS, American Foundrymen's Association, vol. 47, pp. 397-422 (1939).

(c) Boyles, Alfred, *"The Pearlite Interval in Gray Cast Iron,"* TRANSACTIONS, American Foundrymen's Association, vol. 48, pp. 531-573 (1940).

(d) Mehl, Robert, *"The Structure and Rate of Formation of Pearlite,"* TRANSACTIONS, American Society for Metals, vol. 29, p. 813 (1941).

In paragraph 4 of the text the authors state: "It has been established, though, that the transformation of austenite to martensite, pearlite, ferrite, etc., does not bring about a change in grain size. Therefore, by microscopic examination, by observing the different orientations of the pearlite an indication is obtained of the size of the original austenite grain." The transformation of austenite to pearlite, or to ferrite plus pearlite involves a recrystallization. These reactions start with nuclei and growth of new-grains takes place. Mehl has shown (reference *d* above) that near the A_{r1} temperature—"pearlite nucleates preferentially at the austenite grain boundaries but the nodules grow very large transgressing grain boundaries and thus growing into and across grains not yet nucleated, absorbing many austenite grains before impinging upon another growing nodule. Here evidently there are fewer effective nuclei than there are grains of austenite." The nodules of pearlite in this case would be larger than the austenitic grains from which they

[1] Lufkin Foundry & Machine Co., Lufkin, Texas.
[2] Climax Molybdenum Co., Detroit, Mich.

formed. Again Mehl states that near the knee of the S-curve: "In each austenite grain numerous small pearlite nodules form. A nodule forms at an austenite grain boundary and starts its growth inward; before it has grown very far, however, another nodule forms close to it, and shortly the two nodules meet; continued growth then consists in the parallel growth of these two typical nodules toward the center of the grain, producing a structure somewhat similar to the columnar structure formed at the ingot wall in freezing of a pure metal. Here evidently the rate of nucleation is large relative to the rate of growth. The average nodule is much smaller than the austenite grain."

Pearlite grain boundaries cannot be accepted as being indicative of the prior austenite grain. When a fully martensitic sample is etched with special martensitic etch (5 cc. HCl + 1 gr. picric acid dissolved in 100 cc. of ethyl alcohol) grain boundaries are clearly brought out. These boundaries outline the austenite grains developed during austenitization. Martensite forms from the decomposition of austenite grains at low temperatures and the reaction takes place within the boundaries of the original austenite grains.

The authors have not tried to interpret their results with our knowledge of the Fe-C-Si system. They have not recognized the existence of the pearlite interval (references *a*, *b* and *c* above) in which graphite, austenite and ferrite co-exist, or they could have understood why heating samples at temperatures in the vicinity of 1470°F. produced erratic results. The equilibrium diagram for the Fe-C-Si system also shows that the carbon dissolved in austenite increases with an increase in temperature. Generally, the higher the carbon content of austenite the greater is the tendency to crack upon quenching, particularly when most of the austenite transforms to martensite. This explains why there was an increase in "quench-crack formation" when samples were austenitized at 1650°F.

The A_c temperatures determined upon slowly heating the samples can be considered to be fairly accurate determinations of the lower temperature limit of the pearlite interval and the upper temperature limit of the pearlite interval. However, the "A_r" temperatures are merely manifestations of the start of austenite decomposition at the given rate of cooling. They may be correlated with S-curves for the irons. Under similar rates of cooling samples having lower "A_r" temperatures may be considered to have greater austenite stability or slower rates of reaction. The "A_r" points in Fe-C-Si alloys should not be regarded as "critical points."

The authors are to be congratulated for their courage in undertaking such a difficult project. However, I have discussed the paper with the idea in mind that the theory of the heat treatment of gray cast iron should be kept in line with our most recent analyses of such phenomena.

J. S. VANICK[3] *(written discussion):* The authors have made a careful and painstaking study of some of the theoretical factors underlying the heat treatment of plain cast iron. As they point out, the failure for these cast irons to harden upon quenching from 1470°F. is apparently

[3] International Nickel Co., Inc., New York, N. Y.

due to the border-line temperature which this degree of heating represents with respect to transformations. This the authors indicate in paragraph 19 of the text. The theoretical nature of the paper is evident in the application of water quenching because it is well-known that this violent cooling rate promotes the occurrence of quenching cracks. The shop practice or the commercial practice in heat treating cast iron favors quenching it in oil or in air.

The rise in the A_c temperature range is shown to be greater for increasing silicon content and this should be expected because it indicates the greater resistance which silicon exhibits toward resolution. The hysteresis increase with increasing silicon content is apparently related to the same cause, and the effect suggests the probable risk of segregation.

The achievement of maximum hardness by quenching at 1560°F. in irons *A*, *B* and *C* which failed to harden consistently at 1470°F. confirms the practical procedure in the United States of using a temperature sufficiently above the critical to achieve maximum hardness.

Proof of grain growth (paragraph 22) is lacking. The heating time at the low temperature necessary to approach a uniform solid solution condition which is conducive to good hardening quality is better explained by the authors' reference to the inhomogeneity of the "as cast" structure, and the point that the resistance to solution which the carbide in the pearlite offers, and the presence of ferrite, presumably a silical ferrite, contribute a considerable impediment to the attainment of a uniform solid solution on heating prior to quenching.

The normal tendency for cast iron structures to contain such directly opposite elements as ferrite, cementite, pearlite and graphite, closely associated, would account for the reasons why it fails to closely follow the behavior of steel in time and temperature cooling rate functions. When this normal inhomogeneity is exaggerated by the presence of excessive amounts of elements such as silicon or some of the common impurities, then a further departure from the behavior of steel on heat treatment should be expected.

The careful work recorded here registers some of these differences and in that way contributes to a fuller record of the problem of heat treating cast iron.

F. R. MORRAL *(translator's closure):* In behalf of the authors the translator thanks Messrs. Starkweather, Timmons and Vanick for their kind suggestions, criticisms and commendation.

Transatlantic mail is slow at the present time and reply by the authors to the discussion has not arrived in time for publication.

Southern Bentonite in the Steel Foundry

By N. J. Dunbeck*, Eifort, Ohio

1. Southern bentonite is produced principally in Mississippi while western bentonite is produced principally in Wyoming. In both clays, montmorillonite is the dominant mineral. Both have about the same particle size and base exchange capacity. They differ principally in the type ion adsorbed on the surface of the clay particles and thus probably reflect local differences in conditions at time of deposition. The western bentonite carries a high percentage of sodium ions and gives a slightly alkaline reaction. The southern bentonite carries a high percentage of hydrogen and calcium ions and gives a slightly acid reaction.

2. The easiest method of identifying the clays is the fact that western bentonite swells in water to about 10 times its dry volume while southern bentonite swells little more than ordinary clays. As a result, the western clays of this type are known as swelling bentonites, while the southern clays are called non-swelling bentonites.

3. Western bentonite has unusual suspending value and is a useful ingredient in core and mold washes and sprays. Southern bentonite does not have this suspending value and settles rapidly in water.

Difference in Properties Conferred on Sand

4. When mixed with sand, the bentonites differ greatly in the physical properties produced. Each has its own advantages and disadvantages. The southern bentonite is used in non-ferrous work because it gives higher green strength and permeability than any other type of bond and thus works particularly well with fine grained sands. It is used in all types of foundries where intensive mixing equipment is not available, since it is the only type of bond

* Vice President, Eastern Clay Products, Inc.

Note: This paper was presented at a Sand Research Session of the 47th Annual Meeting, American Foundrymen's Association, St. Louis, Mo., April 30, 1943.

which can be cut directly into a heap without danger of bad results. It is used extensively in malleable foundries because its moderate dry and hot strength gives freedom from danger of cracked castings due to sand. It is used in gray iron production systems because it gives high flowability and easy shake-out.

5. But—it is used very little by steel foundries, although it can be a very useful tool in such practice. Southern bentonite has low dry and hot strength as compared to the western type, which has led many steel foundrymen to believe that it has no application in their foundries. It is quite true that, if southern bentonite were used as the only binder, the result would undoubtedly be cuts, washes and scabs. It is suggested that southern bentonite be used only with a full knowledge of its properties and for specific applications where such properties would be of benefit.

6. It is generally acknowledged that southern bentonite gives very much higher flowability than western bentonite. This should be important in steel foundry practice where all sands have notably lower flowability than in iron foundry practice. Southern bentonite is being used to give remarkably fine finish on small steel castings where there is not enough temperature nor weight of metal for dry strength to be important.

Strength Producing Properties Differ

7. Many steel foundries use an all-new-sand-western-bentonite facing and this is excellent practice. The backing sand or system sand is also kept up to strength by additions of western bentonite. Since this sand does not touch the casting, we require only that it have adequate green strength, that it have sufficient flowability to be moldable and that it can be shaken out. Southern bentonite has higher green strength than the western type, it has much higher flowability and definitely lower dry strength.

8. Table 1 indicates differences in these two types of bond. It seems unlikely that the backing sand in an average steel mold reaches a temperature above 800°F. Retained strength is measured, therefore, by heating the sand to that temperature and cooling before testing, to indicate how hard it will bake in a flask. It is difficult to think of any argument which might be advanced why western bentonite should be used in backing sand.

Effect of Mixing Two Types of Bentonites

9. Possibly the chief value which southern bentonite has in a

steel foundry is to regulate the dry and hot strength. If southern bentonite alone is used, dry and hot strengths may be so low as to cause cuts and washes. When western bentonite alone is used, these strengths may be so high as to cause hot cracks and tears. What is more reasonable than to use a mixture of the two which will give dry and hot strength high enough to prevent cuts but still low enough to prevent cracks?

Table 1

DIFFERENCES IN WESTERN AND SOUTHERN BENTONITE

	Tempering Water, per cent	*Green Strength, p. s. i.*	*Dry Strength, p. s. i.*	*Retained Strength, p. s. i.*
5 Per Cent Western Bentonite	2.5	6.38	67.3	55
5 Per Cent Southern Bentonite	2.5	9.99	34.7	20

10. The effect of such mixtures is shown in Table 2. The use of such combinations has definitely reduced hot cracks in large steel castings.

Table 2

EFFECT OF MIXTURES OF WESTERN AND SOUTHERN BENTONITES ON SAND PROPERTIES

Bentonite Mixtures		*Properties*		
Western Bentonite, per cent	*Southern Bentonite, per cent*	*Dry Strength, p. s. i.*	*Hot Strength at 2000°F., p. s. i.*	*Retained Strength at 800°F., p. s. i.*
100		83.4	490	55
75	— 25	54.2	230	45
50	— 50	49.6	190	40
25	— 75	44.8	100	26
	100	37.6	25	20

11. A large midwestern steel foundry has been using a facing sand consisting only of new sand, southern bentonite and cereal binder for several months on castings up to 150 lb. in weight. The sand has better flowability than usual in a steel foundry sand, molding is easier, production consequently greater and finish definitely better than when western bentonite was used. Shake-out is far easier than with former mixes, and lumps in shake-out sand greatly reduced. Dry strength is approximately the same as formerly, due to the cereal binder used, but hot strength is far lower. This has practically eliminated danger of hot cracks due to sand and there has been no cutting or washing.

12. These results suggest the thought that, for steel castings of moderate weight, we may require only an adequate dry strength and not a high hot strength. If true, this means that considerable improvement in usual steel foundry sands can be made by the use of southern bentonite.

Southern Bentonite in Cores

13. It is felt that southern bentonite also has an excellent application in steel foundry cores, where bentonite clay is added for green strength. Such cores made with southern bentonite are strong and hard but have much faster collapsibility than when western bentonite is used. Since we wish some cores to collapse quickly and others more slowly, possibly fire clay should also be used in some cores, since it will give slower collapsibility than either of the bentonites. The suggestion that three types of clay might be advantageously used may meet opposition in that such usage would require closer control and a larger inventory. If the use of such clays would permit a regulation of core properties so as to give a higher percentage of good castings, the added trouble would probably be felt worth while.

TYPICAL APPLICATIONS

14. It seemed desirable to the writer to include some actual foundry sand mixtures in this description so letters were sent to three steel foundries that are using southern bentonite. They were asked to give their original and present mixtures and any comment they might care to make as to the value of southern bentonite in their practice.

Foundry A

15. Steel foundry making tank sprockets, valve bodies, sheave housings and various small castings (Table 3).

Table 3

GREEN SAND—CASTINGS TO 150 LB.

Material	*Original Mix*	*New Mix*
Old sand, lb.	500	500
New silica, lb.	500	500
Western bentonite, qt.	12	
Southern bentonite, qt.		10
Cereal binder, qt.	2	2

Report from Foundry

"We are now making all castings up to 150 lb. in this mix in green sand (Table 1). Our heavier castings, from 150 lb. to 350 lb., are made in the same sand but are washed with silica flour and skin dried. We notice easier mixing and definitely better flowability. We are getting a better peel and finish than we have ever had. Some of the castings going into the cleaning room have no sand on them at all. Our shake-out is far easier and labor cost has been reduced. We have reduced our number of jolts about half. We have less burnt-on sand and have released 12 chippers in the cleaning room. Because of our reduction in riddling and jolting time our muller cannot now keep up with the molders."

Foundry B

16. Steel foundry making tank parts and varied line of steel castings (Tables 4, 5).

Table 4

GREEN SAND—BENCH FACING—CASTINGS UP TO 100 LB.

Material	*Original Mix, per cent*	*New Mix, per cent*
New silica, A.F.A. Fineness No. 58	94.5	94.8
Western bentonite	5.0	1.6
Southern bentonite		3.1
Cereal binder*	0.5	0.5

Table 5

GREEN SAND-FLOOR FACING-CASTINGS UP TO 2500 LB.

Material	*Original Mix, per cent*	*New Mix, per cent*
New silica, A.F.A. Fineness No. 58	75.8	76.0
New silica, A.F.A. Fineness No. 45	18.9	19.0
Western bentonite	4.8	2.7
Southern bentonite		1.8
Cereal binder*	0.5	0.5

Report from Foundry

"Since using southern bentonite we find the following benefits to be especially noticeable:

(1) Freedom from cracks due to excessive hot strength.
(2) Superior flowability making possible a more uniformly packed mold surface, the converse of which, in my

* Special binder added to either western or southern bentonite mixes when extra dry strength is required for particularly difficult jobs.

opinion, causes many more sand inclusions in castings than those created by cutting and washing because of a lower hot strength value.

(3) Superior riddling qualities, saving the molders' time and causing a more uniform distribution of sand over the pattern."

Foundry C

17. Steel foundry making very large truck rear-end housings (Table 6).

Table 6

GREEN SAND FACING

Material	*Original Mix*	*New Mix*
Crude silica, lb.	750	750
Washed silica, lb.	750	750
Old sand, lb.	500	500
Cereal binder, qt.	15	15
Western bentonite, qt.	55	
Southern bentonite, qt.		48
Silica flour, lb.	50	50

Report from Foundry

"The use of southern bentonite in this mix eliminated hot cracks from which we had had serious trouble."

CONCLUSION

18. The writer believes that southern bentonite, intelligently used, can be of real help to steel foundrymen in the following ways:

(1) It gives definitely higher flowability.

(2) It gives easier shake-out.

(3) It gives greater freedom from hot cracks and tears due to sand.

Elevated Temperature Studies of Foundry Cores and Core Making Materials

By Emile Pragoff, Jr.* and C. P. Albus**, Wilmington, Dela.

Abstract

Studies are presented on the hot strength, retained strength and expansion of two types of sand cores commonly used in foundry practice. These types are: (a) new sand cores, bonded with organic binders, no clay being present; (b) black sand cores, prepared from sands containing clay, bonded with organic binders. The conventional method of plotting hot strength curves, with data obtained at intervals of 500°F., is applied to both types of cores. The value of this method with each type core is discussed. Some data, obtained at 100°F. intervals, are similarly presented. The effect of heating cores for various soaking periods at various temperatures is shown to illustrate the rate of breakdown of organic binders as core temperature rises. Data are presented on the retained strength of cores bonded with various binders. In new sand, all these cores reached low retained strength after heating to 800°F. for 10 min. A method for determining hindered expansion of sand cores is described. It is shown to be practical for control testing on black sand cores, but not practical for such tests on new sand cores. A graphic method is used to illustrate the effect of each ingredient, in a core mixture for steel castings, on the hot strength and retained strength of the mixture. Two examples of practical foundry problems, which were solved by the application of sand control, using elevated temperature tests to indicate the logical core-making materials to provide improved core knock-out, are described.

Introduction

1. In recent years, the attention of foundrymen has been directed toward studying the effect of elevated temperatures on molding materials. Foundrymen realized that two molding sands or two core mixtures which showed nearly identical properties at room temperature might have different properties at elevated temperatures.

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2. In 1936 the American Foundrymen's Association initiated some studies of testing methods at elevated temperatures. This work was undertaken at Cornell University under Subcommittee 6b7 of the Foundry Sand Research Committee. Prior to this establishment of a program for studies of molding materials at elevated temperatures, some data had been reported by such investigators as Hudson[1], Dietert[2], Kyle[3], Buchanan[4], and others. It was felt that the knowledge obtained through such a program of work would permit sand control to be extended to such a degree that the mold properties could be controlled at pouring temperatures, and that the resultant castings would be held to closer tolerances, have fewer surface imperfections, and better engineering properties.

3. Already the work of the subcommittee has borne much fruit. It has led to the development of elevated temperature testing methods which are still in the formative stage. The interest of foundrymen in hot strength tests has led to a widespread use of high temperature sand testing equipment, but without a good knowledge of the interpretation of data obtained. It was, naturally, necessary for the Association subcommittee to study the variables encountered in the preparation of specimens, the selection of a suitable specimen, rates of heating, degree of ramming, etc. Almost all of the published work on elevated temperature testing has been related to molding sands; relatively little has been reported on cores. Dietert and Woodliff[5] attempted to relate the collapsibility of foundry cores to the core-making ingredients. Riggan[6] pointed out that hot strength tests are useful tools in the control of core mixtures.

4. The published work, to date, on studies of sand at elevated temperatures has been divided into two logical sections. One of these has been the study of hot strength or compression strength of sand at various temperatures, ranging from the boiling point of water to the pouring temperatures of steel. The other section has been devoted to studies on the measurement of the expansion of sand specimens when heated under so-called shock heating conditions, as well as under slow heating conditions. A considerable portion of the studies under both sections has been published in separate reports of the Subcommittee 6b7[7, 8, 9, 10]. Also, attention has been directed by Buchanan[4] to the effect of ramming density on the expansion of sand. The latter ran a number of expansion

[1] Superior numbers refer to references given at end of paper.

tests on core sands and molding sands containing various binding materials, and concluded that expansion was more directly related to the nature of the sand than to the nature of added ingredients in cores or molds.

5. The purpose of this paper is to present data on the hot strength, retained strength and expansion of some sand cores for foundry use; to present data showing how the selection of core-making materials will affect these properties; and to show how practical use may be made of the elevated temperature tests to control core properties at elevated temperatures.

HOT STRENGTH OF FOUNDRY RAW MATERIALS

6. For some time it has been known that organic binders are low in hot strengths, but possess a degree of resistance to heat. On the other hand, inorganic materials resist heat to a greater degree. We feel that foundry core-making materials may be classed somewhat as follows:

Low Hot Strength Materials
- Vegetable oils such as linseed oil, etc.
- Core oils based on vegetable oils.
- Core oils based on petroleum polymers.
- Cereal binders, gelatinized starch, and dextrin.
- Pine resins.
- Sulfite cellulose binders, either liquid or dry types.
- Coal-tar pitch.
- Synthetic resins.

Moderate Hot Strength Materials
- Certain clays.

High Hot Strength Materials
- Certain clays.
- Mixtures of clays.
- Silica flour.
- Materials such as iron oxide.

7. Under certain conditions, organic materials may combine with inorganic materials to give medium to high hot strengths. This is the case when materials such as the dry binders are used with clays or with natural-bonded sands. Silica flour produces high hot strength in molding sand, as demonstrated by Dietert and Curtis[11].

8. In listing the various binders we should not overlook the cheapest and most abundant binder, water. In the presence of

clays, water produces a considerable effect on not only the green and dry properties, but also on the strength of a sand at elevated temperatures. Water has an appreciable effect upon the strength produced by the cereal binders. It also affects almost all core-making materials.

MATERIALS

9. As in our previous work[12, 13], we have chosen a single sand for most of the tests. This sand was a sub-angular grain silica sand having a fineness number of about 77. It was obtained from New Jersey. This sand was used in all tests on new sand cores. Since this sand is rather finer than the average steel foundry core sand, it would be desirable to make some tests on a coarser sand, as well as on the round-grain shape sands. Where black sand cores are described, a mixture of equal parts of the new sand and a steel foundry synthetic system sand was used. The screen analyses of these sands are given in Table I. The black sand was originally bonded with bentonite, to which some cereal binder was added.

10. The binders tested were:

(1) Raw linseed oil.
(2) Cereal binder—yellow corn variety.
(3) Foundry pitch.
(4) Pine resin—this material may be defined as having a high melting point, about 235°F., low ash, and high gasoline insolubility. It is pulverized to pass 70 per cent through a 200-mesh screen.
(5) Sulfite cellulose binder, dry type, sometimes known as lignin pitch.
(6) Western bentonite.
(7) Southern bentonite.
(8) Ohio fire clay.

TESTING EQUIPMENT

11. The tests reported in this paper were, with few exceptions, made using sand testing equipment of the type which is used by almost all foundries throughout the country. The sand mixtures were prepared in an 18-in. diameter laboratory size intensive mixer of the muller type. This mixer was of the stationary pan, rotary muller type, equipped with the later style plows. Through test it was found that this type plow considerably improved the mixer efficiency. The core specimens were baked for a suitable period in an electrically heated and thermostatically controlled oven.

Table 1

SCREEN ANALYSES OF SANDS

Mesh No.	*New Silica Sand, Per Cent Retained*	*Black Sand*, Per Cent Retained*
6	0.0	0.0
12	0.0	0.2
20	0.1	0.3
30	0.2	0.3
40	0.6	1.4
50	3.2	8.3
70	16.5	40.2
100	46.4	32.8
140	25.4	7.7
200	6.6	1.6
270	0.6	0.4
Pan	0.4	0.6
Clay	0.0	5.9
Grain Fineness No.	77	63
Grain Shape	Sub-angular	Sub-angular

This oven was equipped with rotating shelves; its circulation of air was in accordance with the requirements of the Association. After baking, core specimens were stored under suitable conditions to prevent change in properties prior to hot strength testing. Baking conditions are described later.

12. Elevated temperature tests were conducted using a dilatometer of the Dietert type. This equipment differed only slightly from the type which is supplied to most foundries; it was equipped with a recording, indicating, and controlling pyrometer. The dilatometer is equipped with a 7½ kilovolt-ampere transformer controlling the power input to the heating elements.

13. Expansion measurements were made using the equipment as supplied by the manufacturer; this includes carborundum discs for the protection of the specimen during the determination. It also includes a fused quartz rod which communicates the change of length of the specimen to a dial indicator. Finally, a stop watch is helpful for the accurate timing of tests, although an ordinary pocket watch is satisfactory.

14. The selection of our equipment for determining elevated temperature properties was made after visiting several foundries which had such equipment. This practice is recommended to any foundrymen who contemplate the purchase of such testing equip-

* This is a steel foundry system sand, reconditioned by a mechanical system of reclamation.

ment. Our visits to foundries enabled us to train more efficiently the men who operate this machine.

MODERN CORE PRACTICE

15. In general, modern core practice can be divided into two main groups, as follows:

(1) New sand core practice.
(2) Black sand core practice.

16. New sand cores may be made by using oil alone, or oil plus cereal, or oil plus cereal plus resin binder, or cereal alone. New sand cores may contain additions of clay, such as bentonite or fire clay, but, in general, new sand cores will mean oil-sand. In steel foundry practice, new sand cores frequently contain an appreciable content of silica flour.

17. Black sand cores, on the other hand, mean that the base sand has been used one or more times in the foundry, probably as molding sands. In this group, we have placed black sand cores bonded with pitch, resin, mixtures of pitch and resin, core compounds, etc. Cereal binders may be used, as also may sulfite cellulose waste liquor, either dry or liquid. In this type of core mixture, oil is not used because of the tendency of the fines in the sand to absorb the oil.

18. New sand cores are known to burn out faster, providing easier cleaning, while black sand is often used because of its greater resistance to heat.

19. Foundrymen have known for some time that binders which bake best at low temperatures usually break down readily at moderately higher temperatures, say 100 to 200°F. higher. However, some binders change chemically while baking, or from the heat of the metal during pouring, causing them to break down largely to carbon, which in turn burns slowly. The presence of air in pouring, as in baking, can affect greatly the rate of loss of strength or collapse of any organic binder. By proper control of the elevated temperature strengths of a core, the foundryman should be better able to design a core to do the desired job.

TESTING METHODS

20. The following methods were employed in our tests. Core mixtures were prepared by weighing the ingredients, adding them

to the muller type mixer in the following order: Dry sand, dry binder, oil, water. The laboratory muller would handle quite well a batch of 4500 grams of dry sand. Binders were added on a percentage of the dry sand weight. For example, if we were making a core mixture containing one per cent cereal binder and one per cent of oil with 4 per cent moisture, we placed the materials in the muller in the following order:

Silica sand, grams	4500
Cereal binder, grams	45
Linseed oil, grams	45
Water, grams	180

Some core mixtures dry up more rapidly during mixing. For this reason, the amount of water is varied slightly to give a predetermined desired moisture content. Water was added gradually through a funnel and tubing arrangement, which was arranged to introduce the water into the sand more uniformly.

21. Mixing was done for a period of 3 min. dry and 3 min. wet. In preparing oil-sand containing cereal binder, we mixed the sand with the cereal for 3 min., added the oil, mixed for 3 more min., added the water and mixed for 3 more min.

22. In the preparation of sands containing clay, we mulled the clay with the sand for a period of 2 min. dry and 5 min. wet. The core mixtures were discharged into 2-gal. pails fitted with friction tops. Thirty core specimens were prepared by the double-end-ramming method. These specimens are 1⅛ x 2-in. in dimensions. Thirty of these would fit on each shelf of the core oven. Generally, sixty specimens were charged into the core oven at one time. The core specimens were then baked in the oven under the conditions listed in Table 2. Cores were baked according to recommended practice for the binder employed.

23. After baking, the core specimens were removed from the oven and allowed to cool until warm enough to handle, or less than one hour. They were then placed in glass desiccators over calcium chloride. This was to insure that no pickup of moisture, with attendant loss of strength, occurred.

24. Green and room temperature properties were determined on the various mixtures to observe whether they were in line with the range of desired dry properties. The concentration of binder was selected to give workable cores in the range of the properties normally required in foundry practice. We did not endeavor to

Table 2

BAKING CONDITIONS FOR CORE SPECIMENS

Sand Mixtures	Binder: Type	Binder: Per Cent by Weight	Moisture, Per Cent	Baking Conditions: Hrs.	Baking Conditions: Temp. °F.
New sand	Linseed	1.0			
	Cereal	1.0	4.0	1½	425
	Cereal	2.0	6.0	1	400
	Cereal	1.0	6.0	1	400
	Resin binder	2.0	6.0	1	400
	Sulfite cellulose (dry)	2.0	4.0	1	400
	Pitch	2.0	6.0	1	500
Black sand	Sand alone (no binder added)		6.0	1	400
	Resin	2.0	6.0	1	400
	Pitch	2.0	6.0	1	500
	Pitch	3.0	6.0	1	500
Molding sand	Western bentonite	5.0	4.0	1	400
	Southern bentonite	5.0	4.0	1	400
	Fire clay	10.0	7.5	1	400

produce extremely strong, hard or dense, or extremely weak cores.

HOT STRENGTH TEST PROCEDURES

25. The hot strength test procedures followed in our laboratory are as follows: The temperature of the dilatometer is raised to the desired range by a control of the power input in the transformer. When the furnace is at uniform heat, a sand specimen with carborundum discs properly placed, is introduced into the furnace, lowering the furnace at once. A stop watch is started. At the end of a 10-min. soaking period, pressure is applied to the core specimen at the slowest possible rate. At the present time there is no recommended tentative standard or standard rate of applying pressure to core specimens in hot strength tests. This is one of the weaknesses of the present hot strength tests. The supplier of the equipment suggests a rate of load of 100 lb. per sq. in. per min. We do not know yet whether a given rate is practical. For strong cores a faster rate may be desired than for weak cores. Some experiments have established that our rate of application of pressure is about as shown in Table 3.

26. In the tests on the rate of load, the hand crank was turned at a speed which increased the load at a rate that prevented the needle on the dial from stopping or falling back. The load existing on the specimen at any time was divided by the time in min. to

give the rate of loading of the specimen. Thus, in the first ¼-min. the load had increased from zero to 80 lb. (maximum). In the first ½-min. the load went from zero to 130 lb. (maximum). In the first min., it increased from zero to 265 lb. per sq. in. The rate is thus changing appreciably, especially in the first min. of loading. It is possible that for control work the variable rate of load is satisfactory. For research work on sand properties a fixed rate of load would be desirable. In conducting practical tests on hot strength measurements on a given core mixture, tests were made until consistent readings were obtained. The average of three or more such readings was taken as the hot strength of a given core mixture. In some cases it was necessary to test a considerable number of specimens before reasonable accuracy was assured.

27. Obviously, there is a great opportunity for a core specimen to be injured in handling prior to being placed in the furnace. Care must be taken that only good, sound specimens are used. Although some of these core specimens cracked under shock heating conditions, it is our belief that the results obtained gave some indication of the strength which would be encountered inside the mold.

28. In our work we made no study of the effect of ramming. This has been followed to some degree by investigators such as J. R. Young[10] and Buchanan[4]. It is known from the work of Young that increasing the ramming density of a specimen increases the hot strength. He has shown this to hold true for molding sand speci-

Table 3

TESTS ON RATE OF LOADING OF SPECIMEN*

Time after Initial Application of Load, Sec.	*Observed Load on Specimen, (Lb./sq. in.)*	*Rate of Loading of Specimen, (Lb./sq. in. /min.)*
0	—	—
15	12, 15, 20	48 to 80
30	50, 65, 60	100 to 130
45	107, 145, 150	142 to 200
60	200, 265, 265	200 to 265
75	345, 415, 425	276 to 340
90	535, 595, 665	356 to 443
105	695, 795, 825	397 to 471
120	795, 925, 945	397 to 473

* Specimens were 1¼-in. x 2-in. cylinders—double-end rammed.

mens. We believe that it also holds true, to a considerable degree, for core specimens, especially those which contain clay.

RETAINED STRENGTH

29. By retained strength we mean the strength retained in a core specimen after it has been heated to a given temperature for a given period of time, and then allowed to cool to room temperature. This property represents the resultant strength of the sand and the binder and the heat encountered during baking, pouring, and cooling. This property is a measure of the ease of core knock-out or of mold shake-out. Earlier investigators, such as Dunbeck[14], have selected the temperature of 800°F. for studying retained strengths. In their work they were referring entirely to tests on molding sands. Presumably, 800°F. was considered an average temperature in the mold for some given size of casting.

30. In our work we made retained strength tests over a rather wide range of temperatures from 400 to 1500°F. Usually, a foundryman has some idea of the medium temperature encountered in the core. However, there have been relatively little data published, to date, about the maximum temperatures encountered during the pouring of castings. Later in this paper we will present some information summarizing the published data on this subject.

EXPANSION TESTS

31. The thermal expansion of a sand body is defined by Subcommittee 6c2 on nomenclature as the increase in volume of a sand accompanying an increase in temperature. However, it is reported in terms of increase of specimen length in in. per in. At present, the simplest expansion test which a foundryman can make is that of subjecting a core or molding sand specimen to shock heating conditions; that is, the specimen is placed in the dilatometer at or near the temperature which it will encounter during the pouring of metal. Most of the reported data on molding sand has been obtained on specimens subjected to shock heating at 2500°F. In our tests we employed the shock heating method rather than the slow heating method.

32. Our expansion test was developed by observing what others are doing, applying their experience and, through trial, working out what we consider to be a practical method.

33. The furnace temperature is set at 2500°F., at which tem-

perature the furnace is allowed to remain for 10 min. Then place above and below a sand specimen, two carborundum discs of known expansion (determined by a blank test), raise the furnace to the upper position, placing the specimen on a lower post. Place the fused quartz indicator rod on top of upper (convex) disc. Then lower the furnace over the specimen. Through practice, we found that this operation can be done in 30 sec. (15 sec. for experienced operators), plus or minus 5 sec. If the furnace should stay up longer than 30 sec. the brass support rods are heated too much and the carborundum posts are cooled too much. We find that the smallest possible blank is obtained by following this procedure.

34. As the furnace is lowered, a stop watch is started. Then, for a period of 2 min., a reading of the dial indicator is recorded at 15 sec. intervals. From 2 min. to 5 min., readings are taken every 30 sec. After 5 min., readings are taken every min. up to 12 min.

35. The blank is made with a given pair of carborundum discs, and the same pair is used over again together. Care should be exercised that, in correcting for the blank, the correct blank is applied against the discs used. Care should also be taken that the temperature of the brass supporting post is held, by circulation of cooling water, to a narrow range. Failure to hold the post temperature to a narrow range may increase the size of the blank or spoil the expansion test.

36. In this test the specimen is under what is known as "hindered expansion." It is hindered by the load of the fused quartz rod resting on the specimen. The load amounts, in these tests, to 4 oz. per sq. in.

37. The work of the Association Fellowship at Cornell University has resulted in considerable data on the expansion of molding sands under no load. To date, we have not read of any data published by the Subcommittee 6b7 showing comparative data under both methods. We are not prepared to debate the merits of hindered versus unhindered expansion. We have endeavored to measure the expansion of several different core compositions. We do not feel that the method and equipment we have used is adapted to tests on all types of core mixtures. We feel it is adapted to certain core mixes; *i.e.*, those having sufficiently low contraction to resist deformation during the test. Perhaps, through modification, this equipment and method can be made more valuable in tests on all types of core mixtures.

HOT DEFORMATION

38. In our work we made no determinations on the property of sand known as hot deformation. We have not yet studied this test, nor have we applied it. It is possible that one of the reasons why certain types of black sand cores do not cause hot tears in steel castings is because of the hot deformation provided by the binder. This would be true of those binders which, when heated, go through a thermoplastic condition.

SELECTION OF TEST CONDITIONS

39. In his early work on molding sands, Dietert advocated tests over a wide range of temperatures. Since hot strength tests are quite time consuming, his tests were made at 500°F. intervals. This rather large interval may fail to show something important. The data obtained at high temperatures, reported by Riggan, may fail to show something very significant, which would be revealed by tests at intermediate or lower temperatures. Therefore, we have tried to combine the experience of others in hot strength tests and to cover a range of temperatures not previously reported.

40. A few investigators have reported the temperatures encountered in sand molds. In 1930, Dierker[15] obtained data on temperature gradients in molds for steel castings. Dierker obtained his data by inserting thermocouples at various points in the molds and checking their location after pouring. He determined the temperature, at various time intervals, for steel castings of different dimensions. We have reproduced his data obtained in the casting of an 8 x 8 x 2-in. steel casting. Figure 1 shows the maximum tem-

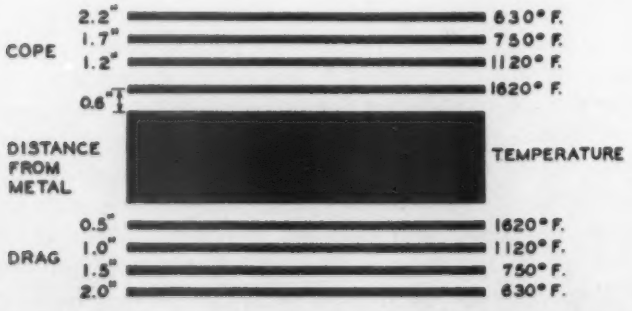

FIG. 1—TEMPERATURE GRADIENTS IN GREEN SAND MOLD 2x8x8-IN. POURED WITH STEEL AT 2750°F. MOLDING SAND COMPOSITION: SILICA SAND, 94 PER CENT; FIRE CLAY, 6 PER CENT; MOISTURE, 2.8 PER CENT[15].

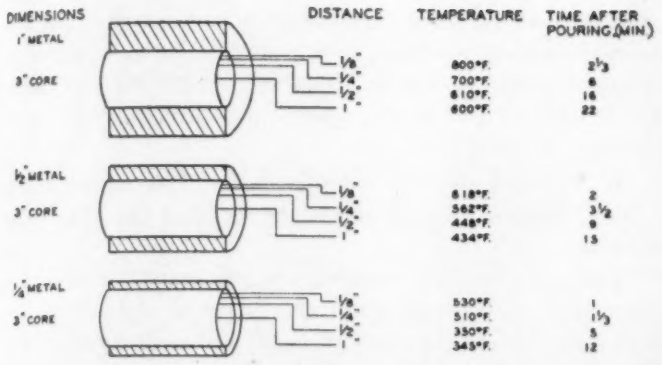

FIG. 2—TEMPERATURE GRADIENTS IN BAKED SAND CORES POURED WITH MAGNESIUM ALLOY AT 1400°F.[19].

peratures reached by molding sand in various parts of the mold. Metal was poured at 2750°F. The maximum temperature reached in the drag, ½-in. from the metal, was 1620°F., at one in., 1120°F., at 1½-in., 750°F., and at 2-in., 630°F.

41. It will be seen from an examination of this diagram that the same temperatures were reached at slightly greater distances from the cope surface of the casting. From the Dierker data, we were not able to report the length of time after pouring at which these maximum temperatures were obtained. These data were obtained in a green sand mold containing 94 per cent Ottawa sand, 6 per cent fire clay, and 2.8 per cent moisture. Castings poured in dry sand molds might raise the temperature appreciably higher or quicker.

42. Some data on temperature gradients were reported by Briggs and Gezelius[16, 17]. Their work was done on steel castings of spherical shape, one of which weighed 106 lb. with 9-in. diameter, the other weighing 31 lb. with 6-in. diameter.

43. Some data along this line were reported by M. A. Scott[18], who reported the heat transfer into molds from iron castings 2½-in. diameter by 12-in. length, weighing 16 lb.

44. To date, we have seen no work temperature gradients in cores for ferrous castings. At this convention a paper is being presented by Peters and Chamberlin[19] on temperature gradients in cores for casting magnesium alloy. Their valuable work reveals the weakness of applying conventional core practice to the casting of magnesium, and we have summarized some of their data in Fig. 2.

45. Magnesium alloys have a low specific heat and low density,

and we find that the cores were heated to rather low temperatures by the various metal sections; presumably, in ferrous castings, the temperatures would be higher. However, we believe that more attention should be paid to the low temperature range from room temperature to 1000°F.

46. In selecting our test conditions, we first varied the temperature of soaking of sand specimens, holding the time of soaking constant.

47. The lack of data on temperature gradients in foundry cores suggested that we first determine hot strength data over a wide range in order to find the point at which new sand cores, as well as black sand cores, rapidly lose strength. It then appeared logical to make tests in the lower temperature range, obtaining data at more frequent intervals. It was, naturally, logical to vary the temperature of immersion in the furnace, as well as the time of soaking. Since the furnace recovery was very rapid, in all cases the time of soak is taken as the time from the lowering of the furnace until the beginning of pressure application.

DISCUSSION OF TEST RESULTS

48. In Fig. 3 is shown a characteristic set of hot strength curves for synthetic molding sands. These data were obtained with a 10-min. soaking period. Data were obtained at 500°F. intervals, from 500 to 2500°F. The curves show that the bonding clays have moderate to low hot strength at room temperature, ranging from 50 to 125 lb. per sq. in. Above 500°F., the hot strength of these molding sands increases rapidly, reaching maximum values at 1500 or 2000°F., depending on the clay. The hot strength of the molding sands falls rapidly thereafter as the clay softens and the plastic zone is reached. At 2500°F. these clays have low hot strengths. These curves confirm the work reported by Dietert and Woodliff[5] and Dunbeck[14], etc.

49. If we apply the same test procedure to determining the hot strength of new sand cores, we obtain a curve such as that shown in Fig. 4. Here it is seen that the hot strength of a typical new sand core falls so rapidly between room temperature and 500°F. that it is difficult to measure accurately. The 10-min. soaking period, considered sufficient to insure uniform heat throughout core, has destroyed the binder. The oil-bonded core falls from 830 lb. per sq. in. at room temperature to 100 lb. per sq. in. at 500°F. A

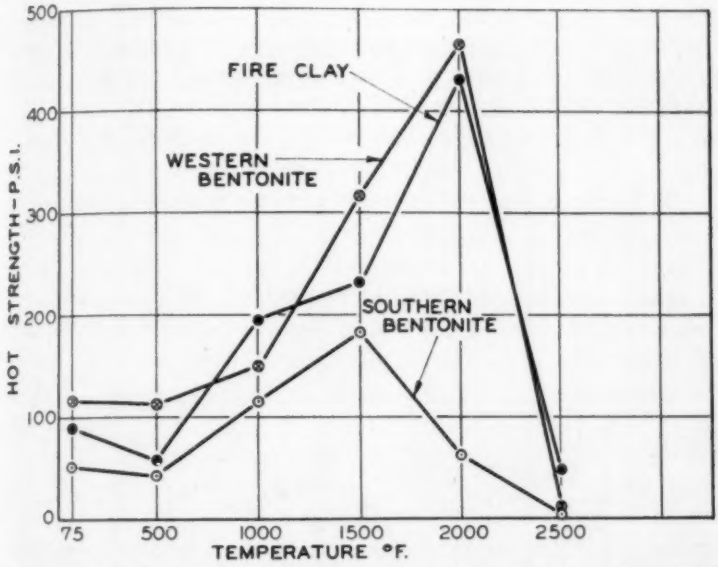

FIG. 3—EFFECT OF TEMPERATURE ON HOT STRENGTH OF SYNTHETIC MOLDING SANDS. SPECIMENS DOUBLE-END RAMMED, 1½x2-IN., SOAKING PERIOD—10 MIN. DATA RECORDED AT 500° INTERVALS.

cereal-bonded core fell even more rapidly in hot strength. A pitch-bonded core rose from 33 lb. at 500°F. to 79 lb. at 1000°F. In general, these core tests meant little because of the very rapid drop in strength over a small temperature range.

50. If we apply this technique to determine the hot strength of black sand cores containing molding sand or clay, we find that we can get a very good picture of the hot strength because measurable strengths are obtained over the entire temperature range. For example, in Fig. 5, are shown data obtained on a black sand comprising 50 parts of reclaimed steel foundry sand and 50 parts of new silica sand. The effects on the hot strengths of sand alone, sand plus 2 per cent pitch, and sand plus 2 per cent resin, are shown.

51. These data show the effect on the hot strengths of the sand alone, which rises rapidly at 1500 to 2000°F., and falls rapidly thereafter. The graph also shows that the hot strengths of the resin-bonded and pitch-bonded cores falls rapidly at temperatures of 500 to 1000°F. The pitch-bonded core reaches a peak at 1500°F. This shows the effect of the pitch, which cokes above 1000°F., providing hot strength in excess of that of the sand and clay alone. In the

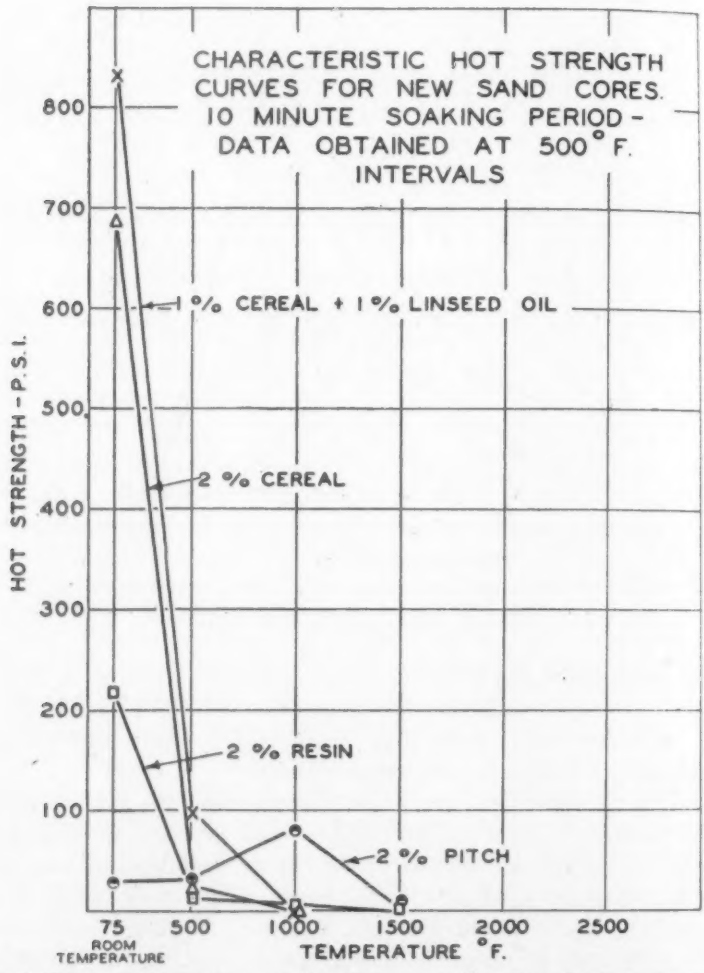

FIG. 4—EFFECT OF TEMPERATURE ON HOT STRENGTH OF NEW SAND CORES BONDED WITH VARIOUS BINDERS.

case of the resin-bonded core, after a temperature of 500°F. is reached this core has a strength about equal to that of the sand. At 2000°F., the cores bonded with pitch or resin show less hot strength than would the sand alone.

EFFECT OF SOAKING TIME

New Sand Cores

52. Since Fig. 4 shows that the conventional method of running

hot strength tests would not give a very clear picture of the hot strength of new sand cores, we then conducted some tests, holding the temperature constant at 1000°F. and varying the effect of time of soaking. These data, which appear in Fig. 6, were determined on four mixtures. These mixes contain, respectively, one per cent cereal plus one per cent linseed oil, 2 per cent cereal binder, 2 per cent resin binder, and 2 per cent pitch binder. These curves show that in a very short time at 1000°F., as little as 2 min., the strength fell quite rapidly. In 5 min. the materials all showed less than 100 lb. per sq. in. in hot strength. The oil and the cereal mixtures fell to a low figure and continued to drop after 2 min., while the resin and pitch cores increased slightly in strength. The resin core reached its second peak at 4 min., and the pitch core its second peak at 8 min.

53. These data serve to show that a temperature of even 1000°F. was sufficient to reduce the hot strength of the cores to a very low point. It was not possible to obtain as great accuracy in the 2-min. soaking time as in a longer period. This is because of

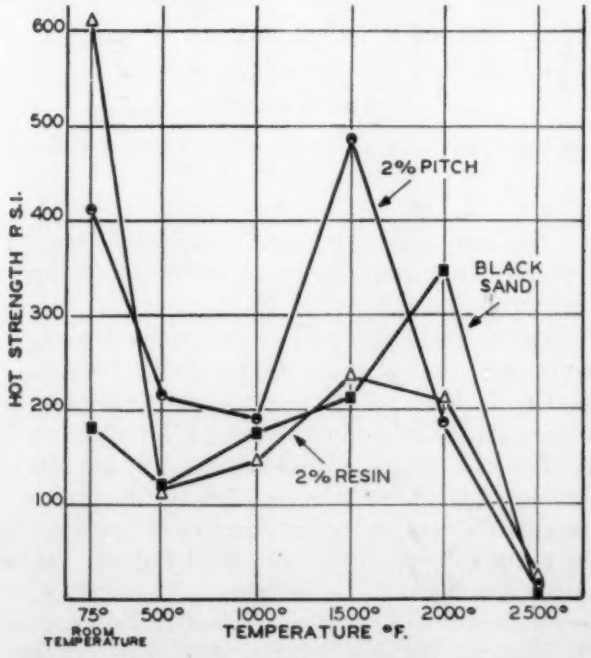

FIG. 5—EFFECT OF TEMPERATURE ON HOT STRENGTH OF BLACK SAND CORES BONDED WITH PITCH OR RESIN. SOAKING PERIOD—10 MIN. DATA RECORDED AT 500° INTERVALS.

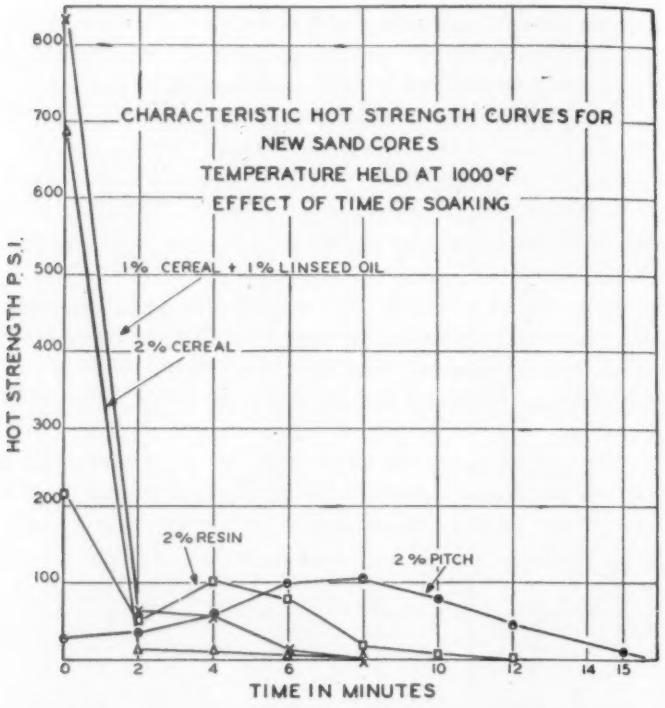

FIG. 6—EFFECT OF SOAKING TIME AT 1000°F. ON HOT STRENGTH OF NEW SAND CORES BONDED WITH VARIOUS BINDERS.

the period required to apply pressure to break the specimen. Thus, such a method is not practical unless we utilize a temperature at which there will be a greater spread of strengths.

54. For more thorough study, we made some tests on new sand cores and the same compositions, holding at 500°F. These data are shown in Fig. 7. Here we obtain a slightly slower rate of breakdown and are able to show the difference between the various compositions. For example, all of these had lost strength to a low point in 8 to 10 min. A 5 min. soaking period at 500°F. would give a fair comparison of the rate of loss of strength of these materials. It should be pointed out that, whereas oils and cereals are commonly used to bond new sand, it is more common to use resins and pitch with sands containing clay. The comparative data are given here simply to illustrate the rate at which these materials alone break down under heat and lose strength.

Black Sand Cores

55. We then endeavored to apply the same technique to black sand cores containing clay bonds plus organic binders. Figures 8, 9, and 10 show the effect of such studies at 500°F., 1000°F. and 1500°F. In Fig. 8, at 500°F., we find that hot strength of the cores, in about 6 to 10 min., dropped to about the same strength as that of the sand alone. It will be seen that the black sand itself provides about the same strength over the entire range of time, whereas the resin has reached its low point in 8 min., and the pitch is still falling at 15 min.

56. We conducted tests at 1000°F. (Fig. 9), and found that the three mixtures lose hot strength rapidly in 2 min., falling, in the case of the resin-bonded core, from 600 to 100 lb. per sq. in. In the

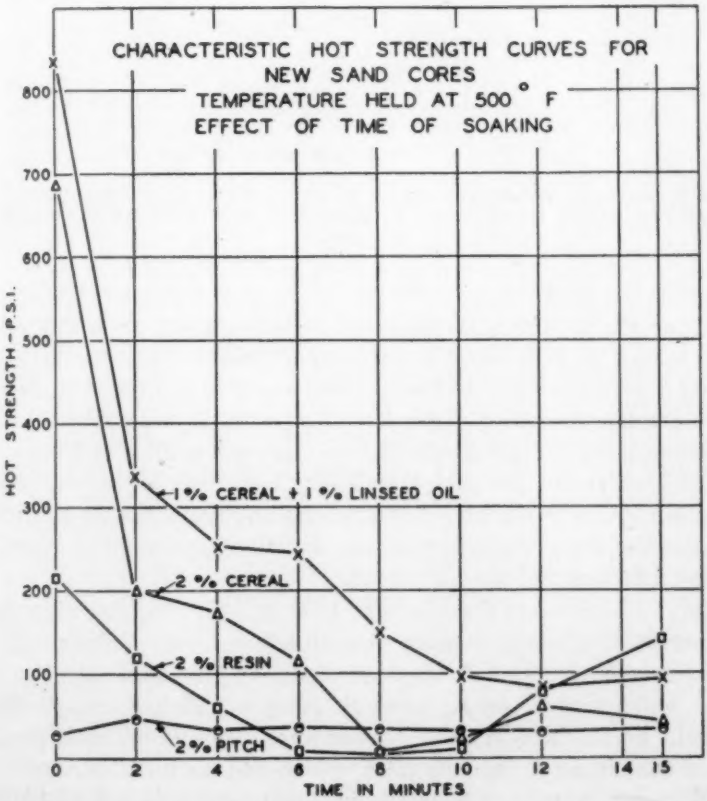

FIG. 7—EFFECT OF SOAKING TIME AT 500°F. ON HOT STRENGTH OF NEW SAND CORES BONDED WITH VARIOUS BINDERS.

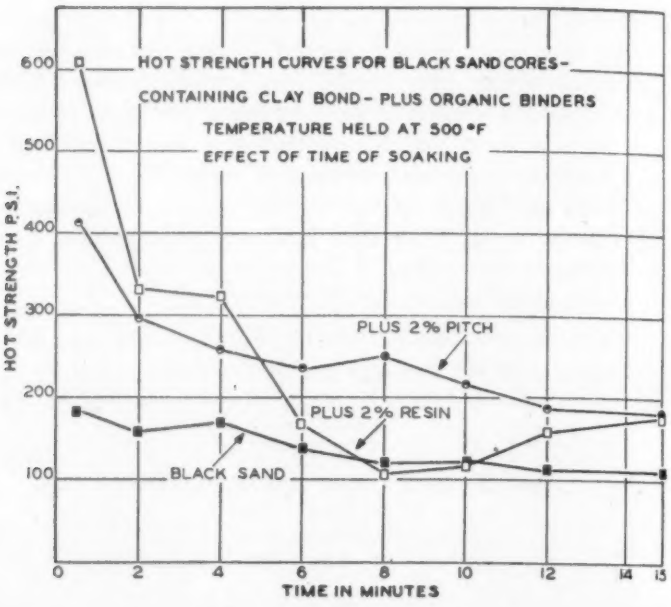

FIG. 8—EFFECT OF SOAKING TIME AT 500°F. ON HOT STRENGTH OF CORES MADE FROM CLAY PLUS ORGANIC BINDER-CONTAINING BLACK SAND TO WHICH PITCH OR RESIN WAS ADDED.

case of the pitch-bonded core, the hot strength fell from 400 to 140 lb. per sq. in. The sand alone fell in hot strength from 190 to 90 lb. per sq. in. It is possible that some of this loss of strength in the first 2 min. is related to loss of moisture from the clay. However, the greater proportion of the loss of strength is in actual softening or weakening of the organic binder. This softening may be related to the melting of the binder, or it may be a thermal destruction of the binder. It is probable that appreciable volatile matter is driven out of the resin or pitch, and that, actually, the resulting material is weaker in compression strength.

57. We repeated these tests at 1500°F. (Fig. 10), knowing that there the clay would begin to pick up hot strength. This proved to be the case and, even in the sand alone, we find that after 4 min. the sand begins to pick up strength, going to a higher strength than would be obtained at room temperature. This is accented in the core containing 2 per cent pitch, which had an initial strength of 400 lb. per sq. in., fell to 140 lb. in 2 min., and then rose to 460 lb. in 6 min. In the case of the resin-bonded core, the hot strength

fell from 600 lb. to about 60 lb. in 2 min., and then rose to about 270 lb. in 8 min., falling thereafter.

58. It is probable that if tests of this sort were conducted in an atmosphere other than that containing air, appreciably different results would be obtained. In this connection, some work was done by Buchanan[4], who found that during expansion tests the presence of coal gas did not affect the expansion, but did affect the retained strength in the resultant baked core.

59. Since we still had not developed a method which could be applied to both new sand cores and to black sand cores, we then tried a test devised as follows: We held the time of soaking constant at 10 min. and determined the effect of temperature at 100°F. intervals over the lower range. These tests for hot strength began at 300°F. and stopped at 1500°F. In Fig. 11 are shown the hot strength data obtained by this procedure on new sand cores. It

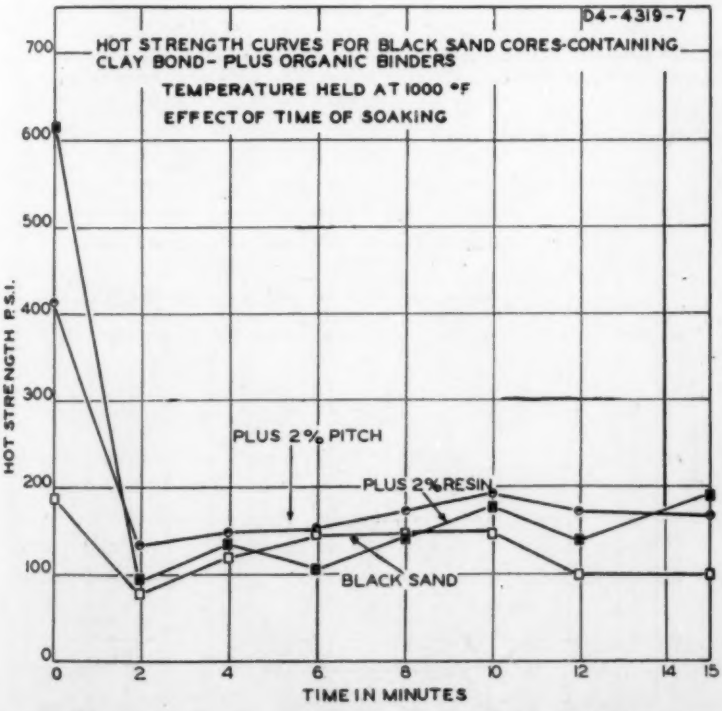

FIG. 9—EFFECT OF SOAKING TIME AT 1000°F. ON HOT STRENGTH OF CORES MADE FROM CLAY PLUS ORGANIC BINDER-CONTAINING BLACK SAND TO WHICH PITCH OR RESIN WAS ADDED.

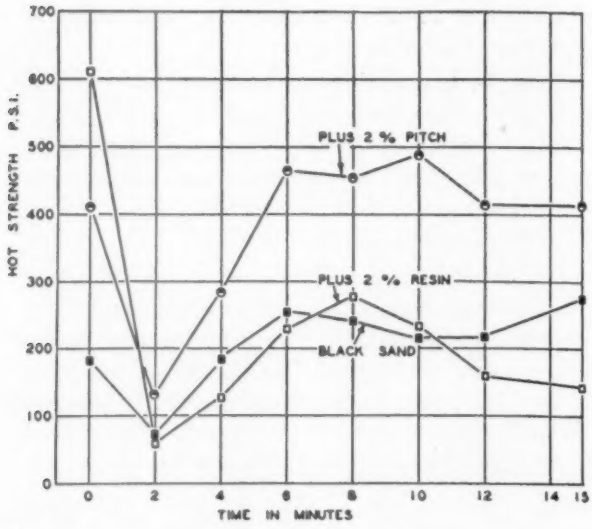

FIG. 10—EFFECT OF SOAKING TIME AT 1500°F. ON HOT STRENGTH OF CORES MADE FROM CLAY PLUS ORGANIC BINDER-CONTAINING BLACK SAND TO WHICH PITCH OR RESIN WAS ADDED.

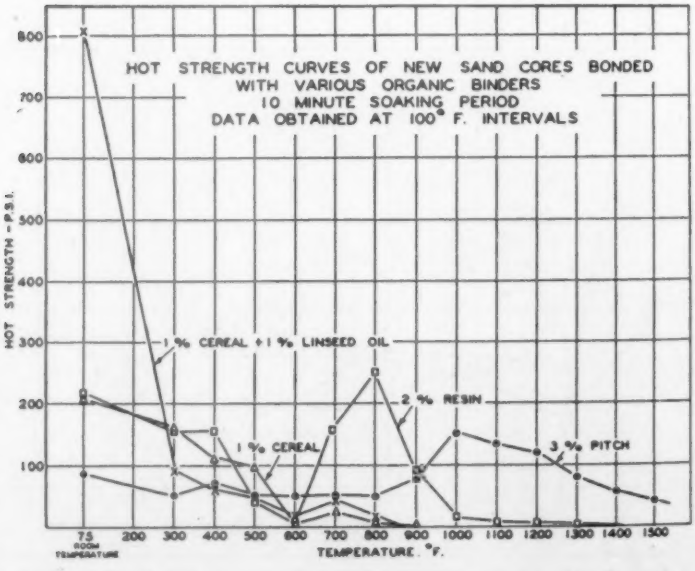
HOT STRENGTH CURVES OF NEW SAND CORES BONDED
WITH VARIOUS ORGANIC BINDERS
10 MINUTE SOAKING PERIOD
DATA OBTAINED AT 100° F. INTERVALS
HOT STRENGTH - P.S.I.
TEMPERATURE °F.
ROOM TEMPERATURE
1 % CEREAL + 1 % LINSEED OIL
2 % RESIN
1 % CEREAL
3 % PITCH

FIG. 11—EFFECT OF VARIOUS ORGANIC BINDERS ON HOT STRENGTH OF NEW SAND CORES.

will be seen that the cereal plus linseed oil mixture had fallen to a very low point at 600°F. There was a slight rise thereafter at 700°F., falling to zero between 800 and 900°F. In the case of the cereal-bonded mixture, the cereal core followed the cereal-oil core. In the case of the resin-bonded core, the strength fell rapidly, approaching zero at 600°F., then rose rapidly thereafter as the resin hardened. The same held true to a similar degree for the pitch-bonded core, with the maximum obtained at about 1000°F.

60. The reason that the pitch and resin have a secondary peak in new sand is obvious. These materials are added to sand in pulverized form. They are not water soluble; hence, develop their bond around the sand grains by melting and flowing and hardening. The hot strength tests may show a tendency of these materials to soften and become plastic at the low range, then hardening as temperatures are reached at which the volatile matter will be driven out and coking will begin. Coking appears to begin in resin-bonded cores at around 700 to 800°F., and in pitch-bonded cores at around 1000°F.

61. We repeated this method of test (Fig. 12) using black sand cores containing, respectively, 2 per cent resin and 3 per cent pitch. The concentration of pitch was increased to 3 per cent in order to obtain a core which would be of slightly higher baked strength initially. While this would exaggerate slightly the results obtained,

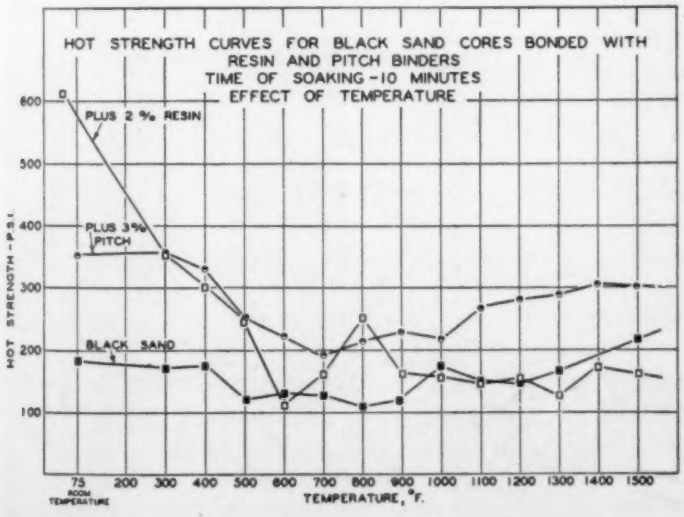

FIG. 12—EFFECT OF PITCH OR RESIN BINDERS ON HOT STRENGTH OF BLACK SAND CORES.

it is not uncommon to encounter cores containing these proportions of materials. The black sand alone did not change much in hot strength from room temperature to 1500°F. It ranged between 120 and 180 lb. per sq. in. At 1500°F. it was rising slowly. In the resin-bonded core, strength fell rapidly, reaching a low at 600°F., then rising briefly to 800°F., falling thereafter, approaching the hot strength of the black sand.

62. This parallels the work obtained with new sand and reported in Fig. 11. In the case of the pitch-bonded core, the strength fell off slowly to 700°F., and then rose slowly thereafter. A comparison of the data in Fig. 12 with that in Fig. 11 illustrates the difference between materials which burn out quickly and those which burn out more slowly.

RETAINED STRENGTHS

63. We feel that retained strength means much to the core removal gang, and should mean much all through the foundry. Anyone who has watched the antics of a gang attacking a machine tool casting to remove a tough core will appreciate the importance of low retained strengths. Any foundryman who has ever cracked a casting or broken an arbor to remove the core is equally aware of its importance.

64. In Fig. 13 are shown typical curves of retained strength in

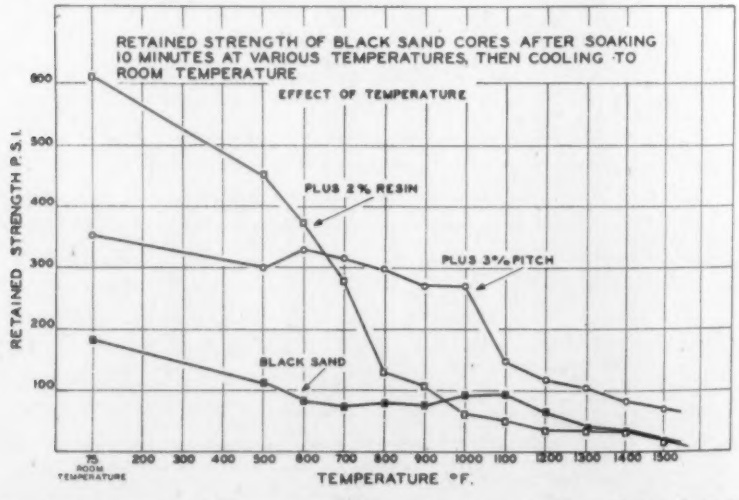

FIG. 13—RETAINED STRENGTH OF CORES MADE FROM BLACK SAND TO WHICH PITCH OR RESIN WAS ADDED.

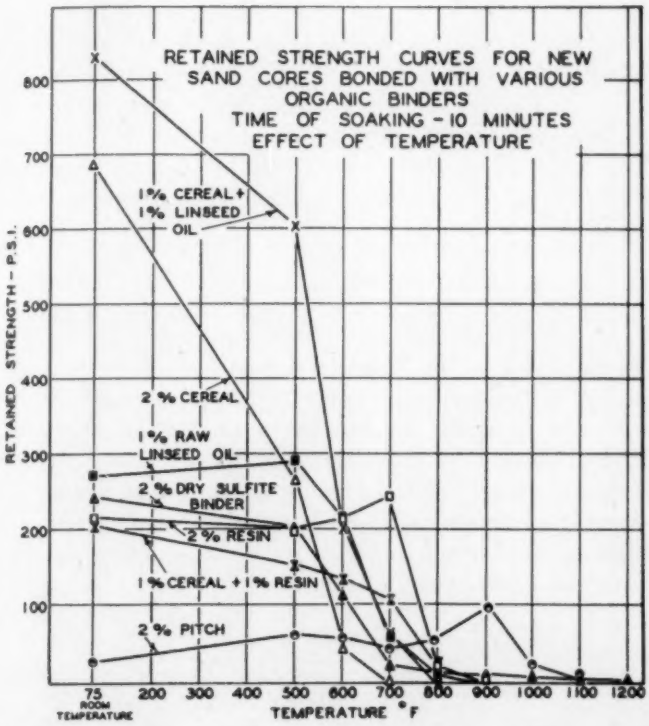
RETAINED STRENGTH CURVES FOR NEW SAND CORES BONDED WITH VARIOUS ORGANIC BINDERS
TIME OF SOAKING - 10 MINUTES
EFFECT OF TEMPERATURE
1% CEREAL + 1% LINSEED OIL
2% CEREAL
1% RAW LINSEED OIL
2% DRY SULFITE BINDER
2% RESIN
1% CEREAL + 1% RESIN
2% PITCH
RETAINED STRENGTH - P.S.I.
TEMPERATURE °F
ROOM TEMPERATURE

FIG. 14—RETAINED STRENGTH OF NEW SAND CORES BONDED WITH VARIOUS BINDERS.

cores heated for 10 min. to a series of different temperatures, then cooled to room temperature. This curve explains why the excessive use of any binder can be dangerous and costly in time and money. It will be seen that the pitch-black-sand core has a higher retained strength than the sand bonded with a resin. The sand itself provides only a limited amount of retained strength after 600°F. is reached. In the pitch-bonded core, the sand must have been heated to about 1200°F. before the strength is down in the range of 100 lb. per sq. in. In the case of the resin-bonded core, this strength is reached at 900°F. A study of this sort can help devise core mixtures having improved knock-out properties. This is especially important in large, blocky-shaped cores surrounded by light metal sections.

65. Comparative data on a number of different binders are shown in Fig. 14. It will be seen that, in new sand, all of the materials produced very little retained strength after a temperature

of 800 to 900°F. had been reached. It is probable that studies on such core compositions conducted in the absence of air would show a somewhat different picture. It is also desirable to make more thorough studies in the temperature range even lower than we have gone. We should, perhaps, start at 300°F., extending to 1000°F. This is especially so in view of our findings that, at 1000°F., all the hot strength of the organic binders has been destroyed.

EXPANSION

66. A number of expansion tests were made on some of the core mixtures prepared. These tests were made following the lines of the procedure described previously. In addition, determinations of dry apparent density were made on the various core specimens. These density determinations were made by accurately measuring the height of the dried specimen to the nearest 0.01-in. The dried specimens were weighed to the nearest 0.1 gram. Density was then calculated and reported as "dry apparent density," in terms of grams per cu. cm. (Table 4).

Table 4

RELATION OF HINDERED EXPANSION AND DENSITY TO COMPOSITION OF FOUNDRY CORES

Mix No.	*New Sand, parts*	*Black Sand, parts*	*Binder — Parts*	*Binder — Type*	*Moisture, Per Cent*	*Dry Apparent Density***	*Expansion*, (Max.)*	*Contraction*, (Max.)*
1	50	50	0		5.9	1.45	+0.023	+0.018
2	50	50	2	Pitch	6.1	1.49	+0.019	+0.018
3	50	50	2	Resin	6.1	1.48	+0.017	+0.015
4	100	0	2	Pitch	6.0	1.40	+0.010	+0.007
5	100	0	2	Resin	6.0	1.43	+0.018	—0.008
6	100	0	1	Linseed oil	4.1	1.48	+0.013	—0.016
			1	Cereal				
7	100	0	1	Linseed oil	4.1	1.46	+0.014	—0.002
8	100	0	2	Cereal	6.0	1.44	+0.010	C
9	100	0	2	Sulfite binder	4.2	1.42	+0.018	+0.007
10	100	0	1	Resin	5.9	1.44	+0.021	+0.005
			1	Cereal				
11	100	0	5	Western bentonite	4.0	1.42	+0.022	+0.017
12	100	0	5	Southern bentonite	4.0	1.42	+0.023	+0.018
13	100	0	10	Fire clay	7.5	1.55	+0.022	+0.018

* Expansion and contraction data reported in in. per in. of specimen length. Specimen heated 10 min. at 2500°F. under 4 oz. load.
** Density data reported in grams per cu. cm.
C—denotes specimen collapsed before test completed.

67. Expansion tests were made by the method described earlier. Data are shown in Fig. 15 for a few of the core mixtures examined. It will be seen that a curve for black sand shows a maximum expansion of +0.023-in. per in., which was reached in about 1½ min. of shock heating. In the next min. or two, an appreciable contraction occurred, which reached a maximum at about 3½ to 4 min. After that, a slight expansion occurred for a period of 2 or 3 min., the specimen then reaching constant dimensions. This curve is characteristic for most synthetic bonded sands using western bentonite.

68. The addition of 2 per cent pitch or 2 per cent resin binder to this sand appeared to decrease the expansion slightly under the conditions of these tests. It is to be remembered that this expansion is a hindered expansion, since the specimen is under a load of 4 oz. per sq. in. The black sand containing pitch reached an early maximum expansion of about 0.019-in. Later, it approached very nearly the expansion of the black sand without any binder added. The black sand containing the resin binder reached a maximum expansion at +0.017-in. per in., dropped only slightly, then reached a second maximum.

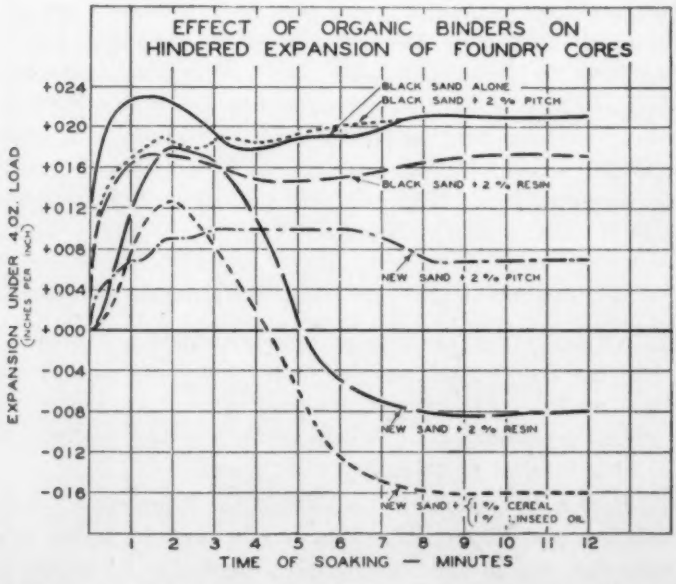

FIG. 15—EFFECT OF SOAKING TIME AT 2500°F. ON THE EXPANSION OF BLACK SAND AND NEW SAND CORES BONDED WITH VARIOUS ORGANIC BINDERS. TEST SPECIMENS LOADED TO 4 OZ.

69. The lower curves with new sand showed a rather different effect and brought out, we believe rather clearly, why the hindered expansion test, as now developed, does not give a true picture of the expansion of new sand cores.

70. For example, the pitch curve with new sand reached a maximum at +0.010-in., decreasing slightly after 6 min. to +0.007-in. In our opinion, this curve would have gone higher except for the fact that the pitch softened under heat, and that the load on the specimen was sufficient to deform the specimen, giving an inaccurate picture of the true expansion. This, we believe, also was the cause for the curve obtained with resin binder and new sand. Here a maximum expansion occurred at +0.018-in. in 2 min., falling very rapidly after 3 min. to reach a maximum contraction, in 8 min., of –0.008-in.

71. The only difference between the curves for pitch and resin with new sand and with black sand was the clay present in the black sand. This clay has sufficient resistance to deformation to prevent the apparent rapid contraction shown with new sand.

72. Also, with an oil-cereal mixture, we obtained a maximum expansion of about +0.013-in. in 2 min. This falls rapidly thereafter, reaching a maximum contraction of –0.016-in. in 8 min. It would appear that while the method employed is suitable for molding sands or for cores containing clay, perhaps it is not adapted for measurements of expansion and contraction on new sand cores. This is because the burning out of the binder shows what appears to be a contraction, but which really may be simply a deformation of the core structure. It is desirable that more data be obtained to assist in settling this question.

PRACTICAL APPLICATION OF HIGH TEMPERATURE STUDIES

73. All laboratory studies on foundry sands are interesting, but, like the Babylonians, we feel that "Only the wisdom which produces workable results is of practical value." By this we mean that unless laboratory studies help produce better castings, they are of little interest or value to foundrymen.

74. How may the foundryman apply the information obtained by high temperature studies? This may be done by trying out, in the coreroom and in the foundry, that which the laboratory tests indicate should be helpful. Two examples are given in the following paragraphs.

75. A gray iron foundry producing heavy machinery castings, such as diesel engine parts, compressors, and the like, had been using black sand and a pitch compound. Their basic mixture, used for both large cores and dry sand molds, was as follows:

System sand, lb.	1,000	(A.F.A. clay content 7.6 per cent)
Western bentonite, lb.	7	(0.7 per cent by weight of sand)
Pitch compound, lb.	18	(1.8 per cent by weight of sand)
Moisture, per cent	5 to 6 (by weight)	

76. This base system sand had an A.F.A. fineness of 51. A green strength of 10 to 12 lb. per sq. in. was desired. This was obtained by the addition of western bentonite. It was found that 10 min. mulling time was necessary to bring out the optimum green strength. Tests showed that this mixture reaches maximum baked strength when baked at 450°F.

77. Their greatest trouble was with lumpy sand after knockout and shake-out operations. They also found that core surfaces frequently were weak, and that the surfaces might be injured during the handling of rather heavy cores. To overcome this difficulty they experimented by adding more pitch compound. Although they obtained greater hardness they also added to their shake-out difficulty. It was then reasoned that they needed to work toward lower retained strength materials in the mixture.

78. Hot strength and retained strength tests were made on two cores bonded with different binders, using the base sand with bentonite in the previously stated proportions. As the result of these tests, a portion of the pitch compound was replaced with a portion of resin binder. Comparative data on the two mixtures are given in Table 5.

79. It will be seen that, whereas the pitch compound core gave a baked compression strength of 556 lb. per sq. in., the replacement of the compound with a resin binder gave 656 lb. per sq. in. strength. Hardness was equal in the two mixes, as was hot strength. A greater difference occurred in the case of the retained strength of the respective core mixes. The pitch compound core had a retained strength after 15 min. heating at 800°F., then cooling to room temperature, of 272 lb. per sq. in. The resin binder gave a retained strength of 131 lb. per sq. in.

Table 5

DATA ON RETAINED STRENGTH OF BLACK SAND CORE MIXES FOR GRAY IRON CASTINGS

	Mix No. 1	*Mix No. 2*	*Mix No. 3*
System sand, lb.	1000	1000	1000
Bentonite, lb.	7	7	7
Pitch Compound, lb.	18	None	9
Resin Binder, lb.	None	18	9
Moisture, per cent	5.6	5.9	5.7
Green compression strength, lb./sq. in.	8.5	11.5	12.3
Baked compression strength, lb./sq. in.	556	656	532
Scratch hardness	75	75	75
Retained strength (after heating for 15 min. at 800°F., then cooling to room temperature), lb./sq. in.	272	131	222
Hot strength (15 min. soaking period), lb./sq. in.			
500°F.	255	212	203
1000°F.	146	114	124
1500°F.	315	223	263
2000°F.	190	200	257
2500°F.	17	19	18

80. By combining suitable proportions of the two binders an intermediate retained strength of lower value was obtained, providing considerably easier core removal. In addition, the resultant core sand had fewer lumps, which were more readily handled by the dry reclamation equipment. Here we see a tangible benefit accruing to the foundryman by proper application in his foundry of principles worked out in the laboratory.

Retained Strength

81. Another example of the value of retained strength data in tailoring a foundry core to the job is illustrated in Table 6. Here we have compared the retained strength of two cores designed for use in casting magnesium alloy. Both core mixtures contain ½ per cent each of sulphur and boric acid, added as inhibitors. Although it is known that these agents have an effect on retained strength, it will be seen that the greater effect is provided by the relative strength of the binder involved. The first core shows an oil-sand mixture bonded with one per cent commercial core oil and one per cent cereal binder. This core had an initial baked tensile strength of 142 lb. and 810 lb. baked compression strength. Its

retained strength, after heating for 10 min. at 500°F., was 712 lb. per sq. in.

82. The other core mixture contained one per cent of a cereal binder and one per cent of a special resin binder developed for magnesium use. This core, which was baked at 300°F., had a baked tensile strength of 100 lb. per sq. in. and a baked compression strength of 522 lb. per sq. in. Its retained strength at 500°F. was 419 lb. per sq. in. It will be seen that the special resin core mixture would be more readily removed at knock-out operations. This proved to be the case in the foundry.

83. The use of retained strength determinations to work out the proper proportions of a core mix for non-ferrous work should prove quite practical and valuable to the non-ferrous foundries. They should study not only the effect of the inhibitors, percentage of binders employed, baking temperatures, but also moisture content of initial core mixtures. Special attention should be paid to the effect of the inhibitors used, since they are known to contribute to high retained strength.

Table 6

DATA ON RETAINED STRENGTH OF CORES FOR MAGNESIUM ALLOY CASTINGS

	Oil-Sand Mix	*Special Resin-Sand Mix*
Silica Sand*	100	100
Core Oil	1	—
Cereal Binder	1	1
Resin Binder	—	1
Sulfur	0.5	0.5
Boric Acid	0.5	0.5
Moisture, per cent	4.2	6.0
Baked 1 hr. at, °F.	450	300
Green Permeability	75	77
Green Compression Strength**	0.8	1.1
Baked Tensile Strength **	142	100
Baked Compression Strength**	810	522
Scratch Hardness	78	73
Retained Strength** (on cooling after heating for 10 min.)		
300°F.	708	372
500°F.	712	419
700°F.	375	238

* All materials measured in parts by weight.
** All strength values are in lb. per sq. in.

Binders

84. Through the efforts of such early investigators as Campbell[20], and others, foundrymen have an appreciable knowledge of the comparative dry strength and preferred baking practice for most of the commonly used binders. It is up to the foundrymen to make use of elevated temperature testing equipment to determine what the various binders will do in the coreroom, and in the mold during pouring.

CORE SAND CONTROL

85. The first step in core control is to provide suitable testing equipment. The next step is to provide personnel who are responsible for its use. After this will come an expenditure of a certain amount of time and money to familiarize this personnel with the high temperature properties of the various core-making materials. The manner in which these materials work together and the manner in which they work against one another should be well established. For example, if it is desired to add silica flour to reduce permeability of a steel foundry core mixture, the man in charge of core properties should know what effect this silica flour addition will have on hot strength and elevated temperature properties of that core mixture.

86. In order to give a simple example of how the various ingredients in a core mixture can be fitted together to give a suitable steel foundry core, a series of mixtures was prepared, data on which are given in Figures 16, 17 and 18. The ingredients in this mixture, in the order in which they were added to the mixer, were:

Ingredient	*Parts*
Silica sand	100
Western bentonite	1
Silica flour	10
Resin binder	1.5
Cereal binder	0.5
Iron oxide	2
Water	6

87. In order to establish the relative baked strength and hot strength produced by each ingredient, we first prepared mixtures containing a single ingredient with water. Thus, the first mixture contained one per cent western bentonite plus 6 per cent water. The

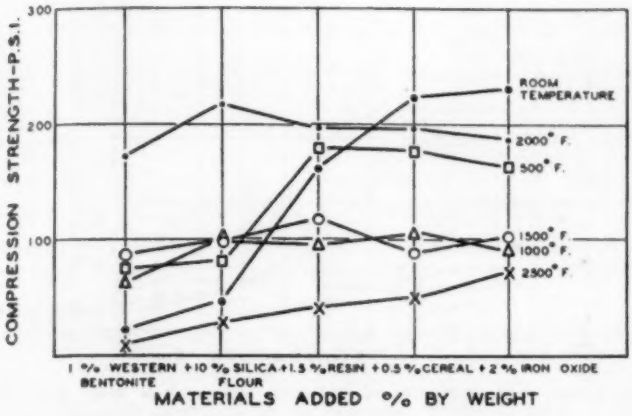

FIG. 16—EFFECT OF VARIOUS INGREDIENTS ON BAKED AND HOT STRENGTHS OF STEEL FOUNDRY CORES.

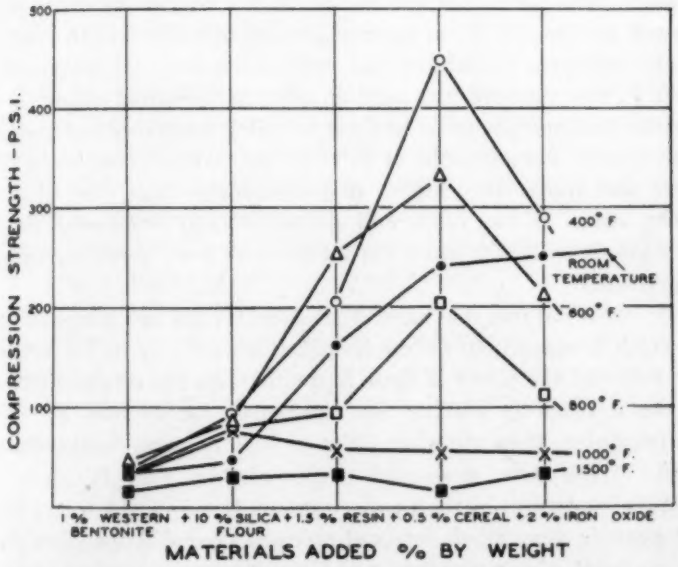

FIG. 17—EFFECT OF VARIOUS INGREDIENTS ON RETAINED STRENGTH OF STEEL FOUNDRY CORES. SOAKING TIME—10 MIN.

second mixture contained bentonite plus the silica flour plus water. The third mixture contained western bentonite, silica flour and resin binder, etc. Baked and hot strength data are given in Fig. 16.

88. It will be seen that the addition of each ingredient increased

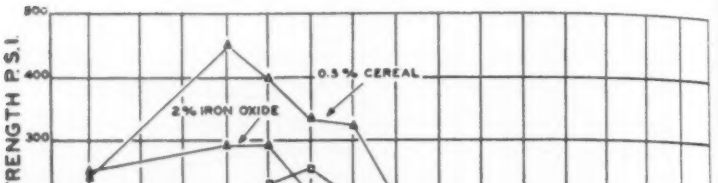

FIG. 18—EFFECT OF TEMPERATURE ON RETAINED STRENGTH OF STEEL FOUNDRY CORES BONDED WITH VARIOUS BINDERS. SOAKING TIME—10 MIN.

the room temperature compression strength of the mixture. Hot strength tests were then performed on the various mixtures which showed that at 500°F. an appreciable amount of strength remained in the mixtures containing the organic binders. However, above 1000°F. this strength was lacking, and the principal change in the various mixtures occurred in those to which inorganic materials had been added. For example, at 2500°F. the mixture containing silica flour was appreciably higher in hot strength than that with bentonite alone. Even resin and cereal slightly increased the hot strength at 2500°F., while the addition of iron oxide increased it appreciably.

89. It is natural that each of these materials had a certain point at which it burned out to lose its baked strength or its hot strength. We followed the effect of these ingredients on the retained strength of the mixture by soaking core specimens for 10 min. at various temperatures, then allowing them to cool to room temperature.

90. These data, presented graphically in Fig. 17, show that western bentonite, in the proportion employed in the mixture, did not provide very much retained strength, nor did the silica flour. At no point of temperature was the mixture containing bentonite and silica flour higher than 100 lb. in retained strength. On the other hand, the resin and cereal binders increased the retained strength up to a temperature of about 800°F. in the case of the resin, and about 900°F. in the case of the cereal. It is significant to note that 2 per cent of iron oxide added to the other ingredients lowered the retained strength rather markedly.

91. In perfecting ideal core mixtures for use in the casting of various metals, the foundry equipped with fundamental knowledge of the raw materials involved will achieve a better core control than the foundry not so equipped. It will require a considerable number of hot strength tests to work out details on all possible core mixtures. However, it would be relatively simple to start with a base sand, such as is used in the coreroom, determining the effect of the normal amount of clay bond and other inorganic constituents employed, then determining the fundamental effect of the inorganic binders only.

92. Where metal sections are light and core sections are heavy, emphasis should be put on the retained strength after heating to relatively low temperatures. On the other hand, where metal sections are heavy, as in the hub of a chilled-iron car wheel, and core sections are not as heavy, emphasis should be laid on determinations at higher temperatures; for example, tests in the range of 1500 to 2000°F. are indicated.

Temperature Gradients

93. In this connection, it is certainly desirable that steel and other ferrous foundrymen have at their command a greater amount of information on temperature gradients in both cores and molds. The lead furnished by Peters and Chamberlin[19] in their recent work on temperature gradients in cores for magnesium castings should be followed for all the other common metals. Although it would be difficult to extend such determinations into the extremely heavy metal section jobs, it would be quite valuable to go up to metal sections of 3 or 4-in. over a range of core and mold sections. The ratio of sand to metal is a large factor in any such work.

SUMMARY

94. The method of measuring hot strength of molding sand cannot be similarly applied to all types of cores. It is applicable to black sand cores containing clay, but not to new sand cores containing organic binders alone.

95. By obtaining data at 100°F. intervals, rather than 500°F. intervals, important facts often can be learned which would otherwise be missed.

96. In new sand cores, the core has lost almost all of the strength provided by organic binders in 10 min. at 1000°F.

97. A 10-min. soaking period may be worth consideration, since it would save much time in testing, and in most tests on cores would tell as much as tests of 12, 15, or 20-min. periods. Less than 10 min. is probably too short to insure uniformity. Hot strength tests made under a given set of conditions indicate only what will occur under those conditions.

98. Retained strength tests are of valuable assistance in selecting materials to give controlled, easy core knock-out.

99. The hindered expansion method for determining expansion of molding sand, as now developed, is not practical for testing new sand cores containing organic binders alone. It is practical for control tests on black sand cores containing clay.

100. More information is needed on temperature gradients in cores during and after the pouring of castings. This information should be obtained for various metals, and a variety of sand to metal ratios.

101. Elevated temperature studies should be made under atmospheres which might be expected to exist within the mold.

102. It would be desirable to reduce the human element as much as possible in hot strength tests by using a constant fixed rate of loading of sand specimen.

ACKNOWLEDGMENT

103. The authors wish to acknowledge the assistance of those members of the Hercules Experiment Station staff who contributed to the work of preparing this paper; to Jack Gentry and Charles Shaw, who performed many of the routine sand tests involved; and to Stanley V. Spoor and his group, who prepared the photographs and illustrations appearing in this paper.

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DISCUSSION

Presiding: J. B. CAINE, Sawbrook Steel Castings Co., Lockland, Ohio.
Co-Chairman: Dr. H. RIES, Cornell University, Ithaca, N. Y.

O. JAY MYERS[1]: Since we are interested in coordinating these tests with actual foundry practice, we should take into account the conditions which exist in the mold when the metal is poured. Three of the main conditions are:

(1) A known weight of metal is being introduced into the mold cavity. This metal weight produced a definite hydrostatic head while it is still molten. This head of molten metal produces a definite pressure.

(2) As soon as the metal is poured, it starts losing heat. Most of this heat emanates through the mold. The sand, therefore, is subjected to a continuous variation in temperature, from the maximum at the pouring temperature to the minimum at room temperature. In the case of magnesium, this would be from about 1400°F. to 80°F.

(3) The cooling period of the metal takes place over a variable length of time, depending upon the temperature of the metal and the temperature of the conditions under which it is cast.

Therefore, the two variables in the process are time and temperature. The constant is pressure. There is a slight variation in pressure when the sand in the mold expands and/or contracts under varying temperature.

Would it not be wise to simulate these conditions in performing hot strength tests? This can be done by placing the baked specimen in the furnace under a constant pressure, as determined by the weight of metal poured and the pressures set up during the expansion and contraction of the core. Maintain this constant pressure for various temperatures within the cooling range of the metal. The time for the collapsing of the specimens can be recorded for these temperaturs. In doing this, we can plot a curve with the temperatures as the abscissa and time as the ordinate. The lines will be constant pressure lines which give a true picture of the variables acting within the mold and serve as a sound basis for the comparison of one facing sand mixture against another.

MR. PRAGOFF: I can only say that as far as we have gone, following the collapse of a core in relation to time, plotting the time of collapse against temperature for a given load, we have applied that with limited success in black sand cores. We have not done much with it in new sand cores, primarily, I believe, because of the very rapid rate of loss in strength.

Another thing that might limit us would be determination of the best load to apply. With a couple of leads based upon some calculations, we might be able to find that very successfully.

MR. O. J. MYERS: We have done some work with several loads, and we find that it is possible to get better checks with a load around 200 lb. than with a load of 100-lb. pressure.

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[1] Wright Aeronautical Corp., Lockland, Ohio.

In aluminum and magnesium foundry sands, we run the hot strength tests as shown by the graphs that Mr. Pragoff and Mr. Albus presented. However, these tests are only run for intervals of ½-min. from zero min. to 2½-min. By doing this, we only get the first portion of the curve, as shown, but we do get the portion of the curve which we think is valuable because the metal does not remain at a high temperature for the 10 min.

The lines shown on such curves, plotting collapsing pressure against soaking time, approach a normal to the ordinate (soaking time). We think the more nearly this curve approaches the normal to that line, the better the substance in the new sand collapses.

MR. ALBUS: I believe the suggestion of applying a load to the specimen under different temperatures is a very good one. As mentioned earlier by Mr. Pragoff, our experiments were carried out under recommended procedures—procedures which are now used by a number of foundrymen—and I think that, until the time when some data are available on constant pressure and temperature versus strength, we might be able to take an average pressure reading against the results obtained under procedures which we used. I believe that we might be able to correlate results with hot strength data that way. Once these other data are obtained, which again brings out the point that we are still rather new on using the dilatometer, each man probably will have to use it to suit his particular practice and set up procedures accordingly. The more data we get along those lines, the more data Mr. Ries and other members of the committee will have from which to suggest and recommend practices for the different types of metals that are being cast.

MEMBER: Were the authors able to discern any difference in resistance to thermal shock when they added these various ingredients to the core sand mixtures, as in Fig. 15?

MR. ALBUS: The best way to answer that question is in taking the steel foundry mixtures, where we had clay and silica sand and added resin and then cereal and iron oxide. I believe it follows that, as we added the organics, we tended to decrease what slight cracking there may have been. We did not have much clay in that mixture, if you will notice, and that probably is why we may not have noticed much cracking. What was there was decreased by these organics. We know that, in similar work on mold facings, organics do decrease cracking at elevated temperatures.

MEMBER: It so happens that the sand I have been testing in a raw or crude state cracks. It has enough natural binder so that I can carry it through to 2500°F. and observe the resistance or what reaction we might get from thermal shock. Now, as I build up the clay content, I remove the cracking and the sand grains will adhere to the specimen. However, if I do the reverse, by introducing cereal I increase the cracking. I have carried it through, and I might have to go as high as 2½ or 3 per cent of bentonite in order to eliminate the cracking.

Then, if I introduce silica flour, I have a slight crack appearing. If I introduce the cereal binder, I have a slight crack and, if I introduce

2 or 3 per cent of silica flour, the crack increases as each addition is made.

I find that, with this sand, I have to get such a high binder—this mineral binder—that the workability of the sand is lost. That is, the bond gets up around 8 or 9 per cent.

MR. ALBUS: Besides clay, we have taken different sands, and we notice that some sands will not have any cracking at 2500°F. Take another silica sand, and that will crack. As far as this effect of additions of minerals is concerned, we have not studied it ourselves. I think Mr. Dunbeck might be able to say something on that. He has worked with clays and sands and he might supply a better answer.

N. J. DUNBECK[2]: Usually we have found that, as we increased the cereal binder, the cracking decreased. We noticed, as you did, that an increase in the amount of clay of any type decreased the cracking.

[2] Eastern Clay Products, Inc., Eifort, Ohio.

Report of Steel Division Committee on Magnetic Tests

Your Committee on Magnetic Powder Tests has attempted to determine the reaction of the industry to this method of testing by circulating a questionnaire containing eight major questions. Sixty-seven replies were received.

The questions contained in the questionnaire, and the replies received and conclusions which may be drawn, are given below.

Question 1—Will you accept orders which state that acceptance of the castings will depend upon the results of magnetic powder tests?

Thirty-eight (38) plants, or 57 per cent of those replying stated that they will accept orders requiring magnetic inspection. The remaining twenty-nine (29) will not accept such orders.

Several of the answers were qualified, in that only orders for certain designs will be considered. Only one of these qualified acceptances was specific. In this case orders will be accepted only "if the term magnetic quality is defined in the specification. Weak adherence of powder, indicating shallow, skin deep defects (inclusions, mechanical dentures, internal strains) not deeper than 1/32 inch in machined surfaces or 1/16 inch deep in rough surfaces is interpreted as not impairing quality."

Question 2—Do you have equipment available which is suitable for making magnetic powder tests?

Thirty-one (31), or 46 per cent, of the replies stated that this equipment is available. Subordinate questions disclosed the information that twenty-eight (28) of these 31 plants use the direct current method of magnetization. Several companies use more than one magnetizing method, so that equipment is available for the solenoid method in thirteen (13) instances and the magnetic yoke is available in five (5) instances.

The currents employed in the foundries with the various magnetizing methods vary considerably. The users of the direct current method reported amperages as low as 200 and as high as 6000. The average range of the majority of the users of this method is 400 to 600 amperes.

The users of the solenoid method reported amperages as low as 10 and as high as 4000. The average range in this case is 300 to 700 amperes.

The users of the magnetic yoke reported amperages as low as 5 and as high as 4000, with an average range of 400 to 600 amperes.

The dry powder method is preferred since 30 of the 31 foundries that have magnetic testing equipment use this method. One foundry uses only the wet method and five (5) foundries use both the wet and dry methods.

Question 3—Have your customers used magnetic powder tests on your castings?

Thirty-two (32) foundries, or 46 per cent of the companies replying have had customers use magnetic tests on their castings.

Subordinate questions disclosed that twelve (12) of these companies did not know the testing method which had been used. In those cases where the methods were known, the data were as follows:

Direct Current	
Dry powder	19 cases
Wet method	5 cases
Avge. amperage	300 to 500 amps.
Solenoid Method	
Dry powder	4 cases
Wet method	4 cases
Avge. amperage	400 to 600 amps.
Magnetic Yoke	
Dry powder	0
Wet method	1 case
Amperage	350 to 450 amps.

The direct current method again is shown to be the most popular method of magnetic testing. This method, with either the dry powder or the liquid suspension, was used in twenty-four (24), or 73 per cent, of the cases reported. The direct current method with only the dry powder was used in nineteen (19), or 57 per cent, of the cases reported.

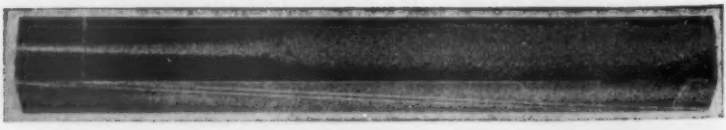

FIG. 2—PHOTOGRAPH OF RESULT OBTAINED THROUGH USE OF THE "PENETRAMETER" ILLUSTRATED IN FIG. 1, SUBMITTED TO COMMITTEE IN PROOF THAT CORRELATION DOES EXIST BETWEEN THE PATTERN OBTAINED AND THE DEPTH OF THE DEFECT.

The answers to these first three questions may also be used to compare the experience with magnetic testing of those foundries which will or will not accept orders where magnetic testing is specified. This tabulation is given below.

	Will Accept Orders (38)	*Will Not Accept Orders (29)*
No experience	1	21
Own tests	29	4
Customer's tests	27	7
Both own and company tests	18	5
Percentage with experience	97.5	27.5

The remaining questions deal with the application of magnetic inspection in production. In many cases only a few of these questions were answered. In other cases the answers were qualified in such a manner that it was impossible to tabulate them.

Question 4—Do you or your customers use magnetic powder testing as (a) the only non-destructive testing method? (b) a tool for exploring defects originally found by radiography to insure complete removal of defects?

Magnetic tests were reported to be the only non-destructive test in 11 instances. Magnetic tests were reported to be used as an aid to exploring defects found radiographical in 13 instances. Several replies stated that while both magnetic tests and radiography were used on the same casting, there was no attempt to correlate the results or to use one method as an aid to the other.

Question 5—Have you found any correlation between the magnetic powder patterns shown on the surface of rough castings and the depth of the defect?

This appears to be a controversial question, since seventeen (17) replies stated there is such a correlation and eighteen (18) replies stated there is none.

One reply to this question included a sketch of a "penetrameter" and a photograph of the result obtained as proof that a correlation does exist between the pattern obtained and the depth of the defect. These exhibits as included in this report are Figs. 2 and 1, respectively.

Question 6—Are you required to explore by chipping or grinding all magnetic powder patterns which indicate defects in rough castings?

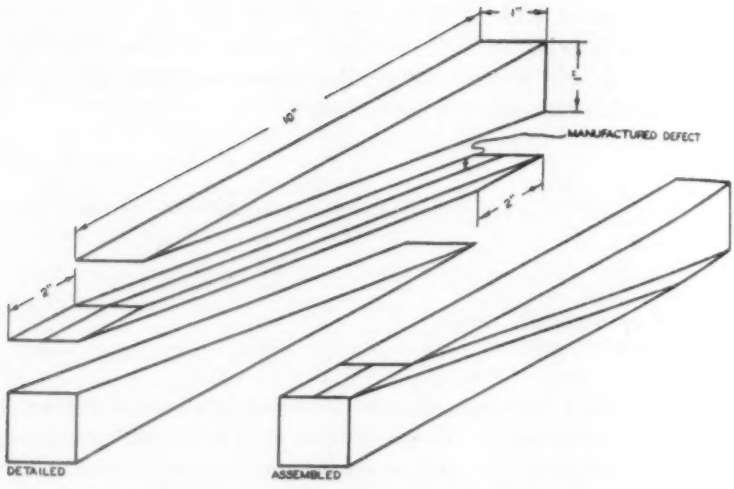

FIG. 1—SKETCH OF "PENETRAMETER" FOR MAGNETIC TESTS, SUBMITTED BY ONE FIRM TO SHOW CORRELATION BETWEEN MAGNETIC POWDER PATTERNS ON THE SURFACE OF ROUGH CASTINGS AND THE DEPTH OF THE DEFECT.

In twenty-two (22), or 78 per cent, of the twenty-eight (28) cases reported, all powder patterns which indicate defects must be explored.

Question 7—If the answer to Question 6 is "no," please explain method or standard which is used to eliminate the exploration of some defects.

Several replies to this question were generalizations which took into account the location of the defect, the general appearance of the pattern, etc. Only one reply gave specific information which can be applied by all. At this particular plant, weak indications (thin lines of loosely adherent powder) which can be blown off with a current of air at a pressure of 1 to 1.5 in. of water are disregarded. This plant uses direct current magnetization at 200-300 amperes for most of its work.

Question 8—Has your experience shown that a magnetic indication on a finished casting is always accompanied by a physical defect?

Twenty-four (24), or 77 per cent of the thirty-one (31) replies received stated a defect is not always present even though magnetic indication may be obtained.

As there are no adequate standards in general use and chipping or grinding is now required for practically all patterns obtained, it appears that this type of inspection causes a great deal of non-essential chipping and welding. In spite of this, most of the people who have had experience with the test will accept orders requiring this method of inspection. Most of those who will not accept orders of this type are those who are not familiar with this method of inspection.

R. A. GEZELIUS, *Chairman*
T. N. ARMSTRONG
J. A. DUMA
W. J. PHILLIPS
A. P. SPOONER

Some Fundamental Relations Within the Cupola

By William Pennington*, Pittsburgh, Pa.

Abstract

While for many years the cupola has been acknowledged as a low-cost melting unit and has been widely used in the castings industry, it is doubtful if anyone, up to the present time, has published the results of a determined effort to analyze the fundamental relations involved in the melting of metal in that unit. The author has made such an attempt. By a careful analysis of the factors involved in cupola melting, he has evolved certain equations, by means of which the melting conditions within the cupola may be ascertained. However, he points out that these equations have certain limitations which he also states. In the forepart of his paper, he outlines the basis for his discussion and then speaks of the chemical reactions involved in the burning of fuel in a cupola. The second part of his paper discusses the relation of carbon gases from the fuel as a means by which the melting behavior of the cupola may be ascertained and refers to Belden's work which he uses in his calculations. The effect of carbon dioxide in the effluent gases derived from limestone in the charge next is discussed. He mathematically shows the relation between the gases evolved from the fuel and those from the limestone. He next discusses carbon burning rate. He derives a number of formulae to determine it and outlines the conditions under which the formulae may be used. Of particular interest is the mathematical derivation of a formula for determining the melting rate. He also gives a typical calculation involving a given series of variables and discusses other melting rate formulae contained in the literature. The next section discusses two families of variables involved in the mathematical calculation of the various factors involved in cupola operation. He divides these variables into two different classes which he labels A-Family and

* Industrial Fellow, Industrial Fellowship sustained at Mellon Institute by The National Radiator Co., Johnstown, Pa.

Z-Family. The variables included in the A-Family are melting rate, cubic feet of air per minute at 0°C. and 760 millimeters pressure, iron-carbon ratio, the per cent CO_2 in the cupola effluent, and the ratio of amount of carbon dissolved in pounds to the amount of carbon burned in pounds. All other variables in cupola operation are classed as being in the Z-Family. To emphasize his analysis of cupola operation, he uses several variables in the Z-Family to show the effect on cupola operation. The author closes his paper with a summary of the many variables which he has considered and the most useful equations evolved in his discussion. He also lists the limitations under which the various formulae may be used.

INTRODUCTION

1. In appearance, the cupola is a simple looking piece of equipment; yet, in its operation, problems are encountered that are perplexing, if not baffling. In examining the materials which go into the cupola furnace, namely, air, fuel, iron (or other metal), and flux, nothing is found which would indicate that the chemistry should be unusual. Still one experimenter may obtain results under a given set of conditions which are diametrically opposed to those found by another apparently under exactly the same set of conditions. This is no paradox, for it is actually impossible to get opposite results where the experimental or operating conditions are precisely the same. An effect is produced by a cause, and, therefore, if two causes are exactly the same, the corresponding effects will also be identical.

2. So often in scientific work such anomalies are attributable to some misunderstanding of terms. For example, it may be said that iron covered with an oxide scale was heated to 1400°F. in a "reducing atmosphere" in which case the scale disappeared, giving a bright-annealed iron, free from scale. A second investigator, on annealing the same kind of iron at the same temperature in a "reducing atmosphere," may find that the scale is not removed, but, on the contrary, more scale is actually formed. Obviously these so-called same conditions are not actually identical. The apparent contradiction is probably hidden in the concept of a reducing atmosphere, because the other factors can be judged satisfactorily by different investigators to be the same. The two atmospheres may be similar in that both will reduce copper oxide to copper, but unlike in that only one will reduce iron oxide to iron under the experimental conditions. In speaking of a reducing atmosphere, one

must indicate the composition or specify the degree of reducing power by stating what will and will not be reduced, or otherwise the expression has little meaning.

3. It is not uncommon to find two cupolas, even in the same foundry, which differ vastly in their behavior where the conditions of operation are "exactly the same." The results can be different only if the conditions of operation are different. There is a cause for the difference in performance which can be found by diligent search. On the other hand, the acceptance of the phenomenon as an unsolvable mystery merely creates confusion and is deleterious to further progress.

4. Many variables must be contended with in the operation of the cupola, but each has its specific effect, an exact mathematical one if you please, on the operation and also on the properties of the iron produced. With such a great number of variables, it is not surprising that agreement cannot always be reached. On the contrary, it is gratifying to find as much agreement as there is.

5. Faith in the fundamental laws of chemistry, such as the law of conservation of matter and the law of definite proportions, has led to the development of mathematical relations within this paper which are applicable to any cupola regardless of its size, location, or who its operator may be.

Basis for Equations

6. The derivation of the mathematical equations has been accomplished by starting with simple facts and applying principles of elementary algebra under certain basic assumptions and limitations. Many of the formulas presented are subject to severe limitations, but are absolutely precise where used under the conditions for which they were derived. Because of the seriousness of the difficulties which may be encountered by improper use of the equations, a set of the important limitations will be given with each equation unless it is thought that the conditions under which the equality holds are quite obvious.

7. Among the data, nearly always recorded in connection with the operation of a cupola, are found the iron-coke ratio and the air-volume in cu. ft. per min. The iron-coke ratio can readily be converted into iron-carbon ratio by dividing by percentage of carbon in the coke and multiplying by 100; the latter ratio is more useful in quantitative chemical calculations. Air-volume is often measured for a standard condition of temperature and pressure so that it may be considered as equivalent to air-weight. Throughout

this paper, the standard condition (abbreviated S.T.P.) used will be 32°F. and 29.92 inches of mercury pressure. Some foundrymen, in recent years, have been measuring the percentage of carbon dioxide (CO_2) in the cupola effluent, taking the results as a reflection of what is transpiring in the combustion zone. The developments made herein will depend primarily upon these three measurements, namely, iron-carbon ratio, air-volume (S.T.P.), and percentage of CO_2.

Fluctuation in Gas Analyses

8. In general, it has been assumed that the cupola is operated with the same uniform charge and with the same kind and amount of air per unit of time until an average equilibrium is reached. Even so there will be a fluctuation in gas analyses owing to the periodic change in height of coke bed arising from the layer method of charging. Then, for equations involving other variables with gas analyses, it is imperative that the average analysis be for one period (time for burning one layer of coke), two periods, etc., or for a long enough time to give reproducible results. The gas sample can be taken some two feet below the top of the burden, but care must be used to prevent a change in composition of the sample once it is taken. Belden[1]* describes a method for taking samples from the combustion zone which may, with some modification, be well adapted for getting the sample near the charging door.

CHEMICAL REACTIONS OF THE FUEL

9. The most important chemical reactions within the cupola are those in which the carbon of the fuel reacts with the oxygen of the air to produce heat in sufficient quantity to cause melting. In determining a heat balance for a specific set of conditions, it matters not whether the correct mechanism of change of state is used as long as all the final products are considered in the amount which has actually been produced. On the other hand, the correct mechanism (*i.e.*, the most probable path of reaction) must be known if one is to consider the chemistry in relation to various factors that are present. In making such a heat balance, it is usually considered that carbon reacts with oxygen in two ways:

(1) $2C + O_2 = 2CO$;

(2) $C + O_2 = CO_2$.

* Superior numbers in both paper and discussion refer to literature cited on p. 1018.

Proper Use of Reactions

10. This scheme should not be interpreted to mean that the oxygen actually reacts with carbon to form both CO and CO_2 directly. At least one of the reactions, from a chemical point of view, may be regarded as being hypothetical, but if viewed in the light of the net result they are both mathematically sound. The scheme is usually accepted primarily because of the simplicity involved. But the gain in simplicity for making the heat balance may be completely overbalanced, if, at the same time, it leads one, who is thinking from a chemical standpoint, to an erroneous mechanism. The use of such a scheme, even for the purpose of making a heat balance, is likely to result in the erroneous opinion that CO_2 is actually formed in two steps: first, the formation of CO as shown in equation **(1)**; and secondly, the subsequent oxidation of CO according to the equation,

(3) $$2CO + O_2 = 2CO_2.$$

11. Should the mechanism consisting of equation **(1)** and **(3)** be correct, it would be necessary, as indicated in **(1)**, for two atoms of carbon to come in contact with one molecule of oxygen simultaneously. Because the atoms of carbon do not occur in pairs in a single molecule, as oxygen does, this contact is indeed unlikely, especially from the standpoint of probability. Inasmuch as CO reacts violently with oxygen at elevated temperatures, it is unreasonable to expect a single carbon atom to accept one of the two atoms of the oxygen molecule and to reject the other which would certainly be in a nascent or more active state. Reaction **(2)** then is to be accepted as the most probable chemical path for the formation of CO_2.

12. At elevated temperatures, the CO_2 thus formed will react with carbon as follows:

(4) $$C + CO_2 = 2CO.$$

The last contention is substantiated by the fact that, above the free oxygen disappearance-line in the cupola, the CO_2 content decreases while the CO content increases. In this way, the formation of both CO and CO_2 has been accounted for chemically as well as mathematically. It is generally agreed among authorities on combustion that reactions **(2)** and **(4)** do represent the most probable chemical equations, as contrasted to hypothetical math-

ematical ones, for the formation of the carbon gases in the cupola effluent.

Types of Reactions vs. Heat

13. Reactions **(1)**, **(2)**, and **(3)** are exothermic and take place with an evolution of heat, while **(4)** is an endothermic reaction absorbing heat. In fact, heat is absolutely required to make the last reaction go in the direction indicated. In certain cooler portions of the blast furnace, the reaction is likely to go in the opposite direction with the precipitation of carbon.

14. For the purpose of making a heat balance, any grouping may be taken from the four reactions if the group will mathematically, possibly not at all chemically, account for the final amounts of all substances. The following combinations can be used for making the heat balance:

1. Reactions **(1)** and **(2)**,
2. Reactions **(1)** and **(3)**,
3. Reaction **(1)** and reverse of **(4)**,
4. Reaction **(2)** and reverse of **(3)**,
5. Reactions **(2)** and **(4)**, and
6. Reactions **(3)** and **(4)**.

The heat balance calculated on the basis of any one pairing will give exactly the same result as any other pairing.

Conceptions of Complete and Incomplete Combustion

15. In discussing combustion, one alludes to complete and incomplete combustion. It is feared that these terms may be confusing. Complete combustion refers to the formation of CO_2 with no CO in the final products. The presence of CO indicates incomplete combustion, the ultimate being CO with no CO_2. On considering these two types of combustion in light of the most probable chemical mechanism, it is apparently paradoxical to find that the combustion is complete before it is incomplete. In other words, CO_2 forms in the first step with the subsequent formation of CO as shown by reactions **(2)** and **(4)**.

16. Conclusions pertaining to the effect of the hot blast are likely to be erroneous if one thinks loosely in terms of complete and incomplete combustion. The first thought may be that, since the increased temperature will cause faster reaction rates, the hot blast will result in the formation of a higher percentage of CO_2

because of the feeling that more complete combustion should be had. In a chain of reactions, if all are caused to go faster, the products of the final reaction of the chain will be produced in increased quantities.

Applications to Hot and Cold Blast

17. To decide upon the true effect of the hot blast, one should consider the effect upon each of the reactions in the most probable mechanism. In the first place, the increased temperature will cause the disappearance of free oxygen to take place in a shorter distance from the tuyeres because of the increased reaction rates. Secondly, the reaction of CO_2 with carbon will proceed further than usual because of the increased temperature and the extra distance the CO_2 has to travel through the incandescent carbon to escape from the coke bed. In both cases, hot and cold blast, complete combustion may be considered to have been attained at some theoretical point in the cupola. As this point is lower in the case of the hot blast, a greater opportunity is had to reduce the CO_2 than in the case of the cold blast.

18. Reaction **(4)** is endothermic; hence it would be desirable to suppress it to get higher combustion efficiency. The suppression may be attained by using larger pieces of coke, up to some optimum size, by using a colder blast, and by charging more iron per pound of coke. The first procedure, in addition to giving combustion economy, will, at the same time, give a hotter iron. The last two methods, in giving higher combustion efficiencies, yield colder irons whose casting efficiency may be considerably impaired if reaction **(4)** is suppressed to a considerable degree.

Reaction Cycles

19. In the ordinary operation of a cupola, the oxygen of the air disappears almost completely in some 12 to 18 in. after it enters the cupola. The tremendous heat produced causes reaction **(4)** to start immediately. However, any CO which is produced on coming in contact with oxygen will be converted again into CO_2. This CO_2 may complete the cycle by again reacting with carbon. In fact, it is possible in the case of a specific carbon atom for the reaction cycle to be repeated several times. Where the oxygen has disappeared, there is no further cycle effect and the mixture tends toward a thermodynamic equilibrium between carbon, CO_2, and CO, which is really never attained owing to low reaction rates at

lower temperatures and the tremendous speed of the flowing gases. Apparently, CO_2 in a gas mixture, traveling as fast as it does in the cupola, does not react to a great extent, in such a short time, with carbon at a temperature somewhat below 2000°F. and, therefore, the concentration of CO and CO_2 will not change appreciably with depth below the charging door. But the sample should be taken low enough in the cupola to insure that oxygen from the charging door is not being drawn into the mixture and yet not low enough to get into the real combustion zone.

RELATION OF THE CARBON GASES FROM THE FUEL

20. In the foregoing discussion, a mechanism for the formation of the various products of combustion has been reviewed. It may be pointed out that in general the amount of CO and CO_2 is governed to a certain extent by the temperature within the cupola and that the more CO there is, the higher the temperature will be. A gas analysis then may serve to indicate what conditions exist within the cupola. CO_2 can be determined by absorption in strong sodium or potassium hydroxide and CO by absorption in acidic or alkaline cuprous chloride. As the percentage of CO can be proved to be a function of the CO_2 content, it is necessary to analyze for CO_2 only. The CO can be calculated by the use of the appropriate mathematical equation.

21. The derivation of an equation to be used for such a calculation is shown below, in which all percentages are expressed on the volume basis. The effluent is assumed to be composed entirely of CO, CO_2, and nitrogen, which means that the air from which it came is made up entirely of oxygen and nitrogen. Both gases, the air and the effluent, contain a considerable amount of argon and lesser amounts of the other inert gases which, as far as all practical purposes are concerned, may be regarded as nitrogen, for it, too, is inert. Water vapor has not been taken into consideration and, therefore, the equation is subject to error to the extent of the amount of water vapor present in the air.

22. The composition of the effluent may be presented in two tabulations:

Effluent (percentage)	*Effluent (volume)*
Let X = percentage CO_2	X = volume of CO_2
Y = percentage CO	Y = volume of CO
$100 - X - Y$ = percentage N_2	$100 - X - Y$ = volume of N_2

The composition has been set up first on the basis of percentage because the unknowns on this basis are the ones that are desired in the derived equation. The tabulation also has been made in terms of volume because of the readiness with which the composition of the air, used to get the effluent, can be deduced in terms of the same unknown quantities. A gas mixture in percentages can be expressed in volumes with the very same numbers, but the mixture given in volumes can be expressed in percentages with the same numbers only in the case where the total volume is taken to be 100.

Volume-Percentage Relations

23. Because one volume of CO_2 is produced from one volume of O_2 (equation **2**), X volume of CO_2 in the effluent will have been produced from X volume of oxygen in the air, but X per cent oxygen (air basis) will not produce X per cent CO_2 (effluent basis). An examination of equation **(1)** shows that one volume of O_2 will yield two volumes of CO; therefore, $Y/2$ volume of O_2 will be required to produce Y volume of CO. The volume of nitrogen is the same in the air $(100 - X - Y)$ as it is in the effluent. The composition of air given in volumes, deduced from the foregoing, is as follows:

Air (volume)*

$$\left.\begin{aligned} X &= \text{volume of } O_2 \text{ for } CO_2 \\ Y/2 &= \text{volume of } O_2 \text{ for CO} \end{aligned}\right\} X + Y/2 = \text{total } O_2$$

$$100 - X - Y = \text{volume of } N_2$$

The percentage of oxygen of the air may be obtained by dividing the volume of oxygen by the total volume of air and multiplying by 100.

24. Thus, $\dfrac{(X + Y/2)\ 100}{X + Y/2 + 100 - X - Y} = \text{percentage of } O_2 \text{ in air.}$

Now if Z be substituted for "percentage of O_2 in air," an equation which expresses the percentage of oxygen of the air in terms of per cent CO and per cent CO_2, in the cupola effluent, will be obtained:

$$\textbf{(5)} \qquad Z = \frac{(X + Y/2)\ 100}{X + Y/2 + 100 - X - Y}.$$

* This presentation has been deduced from the preceding tabulation. The total volume of air constituents is $100 - Y/2$, the volume of air necessary to yield 100 volumes of effluent.

Simplifying,

$$Z = \frac{200\,X + 100\,Y}{200 - Y} \tag{6}$$

or

$$Y = \left(\frac{200}{100 + Z}\right)(Z - X). \tag{7}$$

25. If Z (per cent O_2 in air) were known, its value could be substituted in **(7)** to give a convenient equation for solving for per cent CO when per cent CO_2 was known. Clark[2] gives a tabulation of some 500 analyses of air made in many different parts of the world by as many as eight different analysts. Their average value for oxygen is 20.93, which, on being substituted in **(7)**, yields:

$$Y = 1.654\,(20.93 - X). \tag{8}$$

Determining Oxygen in Air

26. In most cases, equation **(8)** would be quite satisfactory, but it is to be preferred that Z be fixed by actually analyzing the cupola effluent and substituting the values of per cent CO and per cent CO_2 in **(6)**, thereby solving for Z under the conditions for which the equation is to be used. For this purpose, the data of Belden[1] are considered to be excellent. He used a standard 36-in. Whiting cupola lined down to 27-in. diameter and, for the sake of simplicity and convenience, studied the combustion conditions by burning the coke with no iron being charged. Gas samples were collected in five horizontal planes, *A*, *B*, *C*, *D*, and *E*, *A* being 4-½ in. above the lower edge of the tuyeres and the other planes higher in the order given. There was 6 in. between successive planes, except between *D* and *E*, which were removed from each other by 7-½ in. Thus it was 25-½ in. from *A* to *E*. Locations 1, 2, and 3 in each plane were on a radius of the cross-section of the cupola such that they were 4-½ in. apart, 1 being on the central axis of the cupola.

27. In taking the samples in the combustion zone, it was to be expected that at some points free oxygen would be present. Inasmuch as one volume of oxygen forms one of CO_2, the percentage of O_2 has been added to that of CO_2, corrected for deviation from the ideal gas law, to obtain a value of X. The percentage of CO as obtained by Belden was substituted directly for Y in equa-

tion **(6)**, along with the value of X as obtained above. Thus the value of Z was obtained. To make the value as accurate as possible, the percentage of CO_2 was corrected, as mentioned previously, for its deviation from the ideal gas law. Under this particular condition of temperature and pressure, the correction may be made by multiplying the percentage of CO_2 by 1.005. The deviation of the other gases is very small and need not be considered.

28. In Table 1, the data for percentages of CO_2, O_2, and CO are from the work of Belden[1]. The corrected percentage of CO_2, the sum of corrected CO_2 and O_2, and the values of Z obtained from equation **(6)** are also shown in this table.

29. The agreement of the percentage of oxygen in the air as obtained in the five experiments is all that could be asked. All average values for the different experiments lie within the range 20.67 to 20.83 with an overall average of 20.75. The fact that such good agreement is obtained from experiment to experiment and that the average value of 20.75 is in fair agreement with Clark's average of 20.93 is taken as positive proof of the validity of equation **(6)**. To obtain the best value for expressing CO in terms of CO_2, it is only a matter of selecting the best value for Z. It is clear that the methods used for ordinary gas analysis of a cupola effluent will probably give values closer to those of Belden than to those obtained by the analysts whose more precise results were reported by Clark. Therefore, the value of 20.8 (rounded off from 20.75) is selected in preference to the other value of 20.93 and, therefore, equation **(9)**, obtained by substituting $Z = 20.8$, is to be preferred over equation **(8)**.

(9) $$Y = 1.656\ (20.8 - X).$$

Limitation: Not to be used if limestone is in the charge.

30. As stated in the limitation note, the equation should not be used where limestone or, for that matter, where any other carbon-gas producing substance is charged into the cupola. If the equation is used anyway, the calculated CO-content will be low by as much as 4 per cent of its total where limestone to the extent of 25 per cent of the total carbon is used. Another equation will be offered which is more suitable for use where limestone is to be considered.

Table 1

ANALYSES OF CUPOLA GASES

EXPERIMENT 1

Plane	*Location*	CO_2	O_2	*(Y)* CO	*Corr.* CO_2	CO_2 *(X) (Corr.)* $+ O_2$	*(Z) per cent* O_2 *in Air*
A	1	5.9	0.0	25.1	5.93	5.93	21.13
	2	10.3	9.5	1.6	10.35	19.85	20.82
	3	1.1	19.6	0.0	1.11	20.71	20.71
B	1	7.9	0.0	21.3	7.94	7.94	20.81
	2	18.3	1.3	1.7	18.40	19.70	20.73
	3	10.4	10.1	0.1	10.45	20.55	20.61
C	1	7.0	0.0	23.2	7.04	7.04	21.09
	2	15.0	0.2	8.6	15.08	15.28	20.36
	3	15.2	4.9	.4	15.28	20.18	20.42
D	1	6.3	0.0	24.0	6.33	6.33	20.83
	2	12.1	0.0	14.8	12.16	12.16	21.12
	3	17.6	0.7	4.1	17.69	18.39	20.87
E	1	6.7	0.0	24.1	6.73	6.73	21.35
	2	10.6	0.0	16.6	10.65	10.65	20.67
	3	16.5	0.3	6.8	16.59	16.89	21.00
						Average	20.83

EXPERIMENT 2

A	1	8.8	0.0	19.5	8.85	8.85	20.61
	2	10.3	0.1	16.9	10.35	10.45	20.63
	3	2.5	18.1	0.0	2.51	20.61	20.62
B	1	15.6	0.5	8.3	15.68	16.18	21.21
	2	11.8	0.1	14.7	11.86	11.96	20.84
	3	14.0	6.2	0.7	14.07	20.27	20.69
C	1	13.9	0.2	10.9	13.97	14.17	20.75
	2	14.2	0.0	13.1	14.28	14.28	22.29
	3	15.0	4.7	1.5	15.08	19.78	20.69
D	1	11.3	0.0	15.3	11.36	11.36	20.58
	2	11.0	0.1	15.8	11.06	11.16	20.69
	3	17.0	0.7	4.9	17.09	17.79	20.75
E	1	9.5	0.0	18.3	9.55	9.55	20.58
	2	10.4	0.0	16.7	10.46	10.46	20.52
	3	15.8	0.0	7.8	15.88	15.88	20.58
						Average	20.80

Table 1 (Continued)

EXPERIMENT 3

Plane	*Location*	CO_2	O_2	(Y) CO	Corr. CO_2	CO_2 (Corr.) + O_2 (X)	(Z) per cent O_2 in Air
A	1	17.1	0.7	4.9	17.19	17.89	20.85
	2	11.0	0.4	15.5	11.06	11.46	20.82
	3	3.1	17.6	0.0	3.12	20.72	20.72
B	1	12.2	0.0	14.0	12.26	12.26	20.71
	2	13.1	0.3	12.0	13.17	13.47	20.71
	3	8.1	12.4	0.0	8.14	20.54	20.54
C	1	12.9	0.0	13.1	12.97	12.97	20.89
	2	13.8	0.2	11.2	13.87	14.07	20.84
	3	13.2	7.3	0.5	13.27	20.57	20.87
D	1	10.8	0.0	16.3	10.86	10.86	20.70
	2	13.3	0.1	12.3	13.37	13.47	20.91
	3	16.9	2.6	1.8	16.99	19.59	20.68
E	1	10.1	0.0	17.3	10.15	10.15	20.58
	2	—	—	—			
	3	16.8	0.0	5.4	16.89	16.89	20.13
						Average	20.71

EXPERIMENT 4

A	1	16.8	0.3	6.0	16.89	17.19	20.81
	2	6.0	0.5	24.2	6.03	6.53	21.19
	3	9.5	10.6	1.1	9.55	20.15	20.81
B	1	14.0	0.0	11.1	14.07	14.07	20.77
	2	9.2	0.0	18.8	9.25	9.25	20.58
	3	12.4	7.8	1.1	12.46	20.26	20.93
C	1	11.7	0.0	14.7	11.76	11.76	20.63
	2	8.2	0.7	19.7	8.24	8.94	20.84
	3	16.6	2.8	1.2	16.69	19.49	20.21
D	1	9.1	0.0	18.9	9.15	9.15	20.54
	2	9.1	0.0	19.8	9.15	9.15	20.73
	3	16.5	0.1	6.9	16.59	16.69	20.86
E	1	7.1	0.3	21.5	7.13	7.43	20.37
	2	8.5	0.0	19.0	8.54	8.54	19.93
	3	12.1	0.2	14.1	12.16	12.36	20.88
						Average	20.67

Table 1 (Continued)

EXPERIMENT 5

Plane	Location	CO_2	O_2	(Y) CO	Corr. CO_2	CO_2 (X) (Corr.) + O_2	(Z) per cent O_2 in Air
A	1	11.6	0.0	15.5	11.66	11.66	21.04
	2	13.8	0.1	11.6	13.87	13.97	20.99
	3	7.3	13.1	0.3	7.33	20.43	20.76
B	1	15.6	0.3	7.5	15.68	15.98	20.50
	2	10.3	0.0	17.2	10.35	10.35	20.73
	3	12.7	7.8	0.4	12.76	20.56	20.80
C	1	14.1	0.5	10.1	14.17	14.67	20.77
	2	—	—	—	—	—	—
	3	15.1	4.8	1.4	15.18	19.98	20.83
D	1	11.4	0.0	15.8	11.46	11.46	21.02
	2	12.1	0.0	14.2	12.16	12.16	20.73
	3	16.6	0.2	6.4	16.69	16.89	20.75
E	1	9.7	0.0	18.1	9.75	9.75	20.67
	2	10.9	0.0	16.0	10.95	10.95	20.60
	3	14.9	0.0	8.8	14.98	14.98	20.27
						Average	20.75

Overall Average 5 experiments = 20.75

CARBON DIOXIDE FROM LIMESTONE

31. Limestone decomposes at 1500°F. into lime and carbon dioxide. It is expected that the decomposition would be complete before it reaches a zone of such high temperature that the CO_2 would react with the carbon at an appreciable rate. It is therefore assumed that the CO_2 produced in the decomposition of limestone occurs in the effluent gas as such. A chemical determination for CO_2 will, of course, include the CO_2 from the limestone as well as that from the combustion of coke. It is possible, however, to calculate the amount obtained from the limestone if the total CO_2 and CO are known.

32. An equation for this purpose can be derived in exactly the same way that **(7)** was. The scheme follows:

Effluent (percentage)	*Effluent (volume)*
Let X = percentage CO_2 (total)	X = volume CO_2 (total)
M = percentage CO_2 from stone	M = volume CO_2 from stone
Y = percentage CO	Y = volume CO from air
$100 - X - Y$ = percentage N_2	$100 - X - Y$ = volume N_2 from air
	$X - M$ = volume CO_2 from air

Air (volume)

$X - M$ = volume O_2 for CO_2

$Y/2$ = volume O_2 for CO

$100 - X - Y$ = volume N_2

Air (percentage)

Let Z = percentage O_2 in air

$$\frac{(X - M + Y/2)100}{X - M + Y/2 + 100 - X - Y} = \text{percentage } O_2 \text{ in air.}$$

On equating the two values for percentage of oxygen,

$$\frac{(X - M + Y/2)100}{X - M + Y/2 + 100 - X - Y} = Z. \tag{10}$$

Substituting 20.8 for Z and simplifying,

$$M = 1.263X + 0.763Y - 26.26. \tag{11}$$

33. Limestone is usually added in some definite proportion to the weight of coke, in which case the percentage of CO_2 resulting from the decomposition can be calculated on a different basis from that just found. A convenient equation can be derived by adding the stone in an amount which is a definite percentage of the weight of carbon associated with it:

Let W = ratio of limestone to carbon of coke (weight basis); as 12 per cent of stone is carbon,
$0.12W$ = ratio of carbon of stone to carbon of coke.

Each of the carbon compounds in the cupola effluent gas contains one carbon atom only. Hence the ratio (weight basis) of the carbon of the limestone to the carbon of the coke will equal the ratio (volume percentage basis) of CO_2 from limestone to carbon-gases from coke. Then

$$0.12W = \frac{\text{per cent } CO_2 \text{ from stone}}{\text{per cent } CO_2 \text{ from coke} + \text{per cent CO from coke}},$$

but M = per cent CO_2 from stone and $X - M + Y$ = *per cent* CO_2 + *per cent CO* from coke, which may be substituted in the above expression to give

$$M = 0.12W\ (X - M + Y), \tag{12}$$

from which it may be found that

$$M = \frac{W(X + Y)}{W + 8.33}. \tag{13}$$

Of the two equations, **(13)** is to be preferred over **(11)** because it gives the values from more direct measurements.

RELATION OF CARBON-GASES WHERE LIMESTONE IS USED

34. If the values of M from **(11)** and **(13)** are equated, an equation can be derived which will make it possible to calculate the percentage of CO where the amount of limestone relative to carbon of the coke and where the total percentage of CO_2 are known. In other words, Y will be given in terms of W and X.
From **(11)** and **(13)**,

$$1.263X + 0.763Y - 26.26 = \frac{W(X + Y)}{W + 8.33}, \tag{14}$$

which gives:

$$Y = \frac{218.832 - 10.5216X + 26.26W - 0.2626WX}{6.355 - 0.2374W}. \tag{15}$$

35. For the special case where the limestone is added in an amount equal to 25 per cent of the carbon of the coke, 0.25 may be substituted for W in **(15)** to get, after simplifying,

$$Y = 1.682(21.28 - X). \tag{16}$$

Other equations of this type may be had for cases in which other amounts of limestone are used by substituting the corresponding values of W in **(15)**.

CARBON-BURNING RATE

36. In many considerations concerning the cupola, it is desirable to know the rate of burning of carbon, not so much for the rate of disappearance of carbon itself, but because there is a simple relation between it and the melting rate of the iron. Therefore, if the carbon-burning rate is known, the melting rate can be calculated directly.

37. An algebraic expression of burning rate of carbon may be given in terms of air-volume under standard conditions of temperature and pressure and percentage of CO_2 in the cupola effluent in an equation developed by the following derivation:

Let B = lb. of carbon burned per hr.;

as all carbon is burned to CO or CO_2,

(17) *B=(lb. of C to CO_2 per hr.)+(lb. of C to CO per hr.).*

According to the law of definite proportions, the weight of carbon which reacts to form CO_2 is proportional to the weight of oxygen with which it reacts, then

$$\textbf{(18)} \qquad \frac{\textit{lb. of C to } CO_2}{\textit{hr.}} = K_1 \left(\frac{\textit{lb. of } O_2 \textit{ to } CO_2}{\textit{hr.}} \right).$$

Where K_1 is a constant of proportionality which is equal to the weight of a number of atoms of carbon divided by the weight of a number of oxygen molecules which react with them:

$$K_1 = \frac{\textit{wt. of one atom of C}}{\textit{wt. of one molecule of } O_2}$$

$$K_1 = \frac{12}{32} = 0.375 .$$

Likewise,

$$\textbf{(19)} \qquad \frac{\textit{lb. of C to CO}}{\textit{hr.}} = K_2 \left(\frac{\textit{lb. of } O_2 \textit{ to CO}}{\textit{hr.}} \right).$$

$$K_2 = \frac{24}{32} = 0.750.$$

Substitutions from **(18)** and **(19)** into **(17)**, together with the values for K_1 and K_2, give

$$\textbf{(20)} \qquad B = 0.375 \left(\frac{\textit{lb. of } O_2 \textit{ to } CO_2}{\textit{hr.}} \right) + 0.750 \left(\frac{\textit{lb. of } O_2 \textit{ to CO}}{\textit{hr.}} \right)$$

Let L = total lb. of O_2 per hr.,
Y_1 = fraction of O_2 to CO_2,
$1-Y_1$ = fraction of O_2 to CO,
LY_1 = lb. of O_2 per hr. to CO_2,
$L(1-Y_1)$ = lb. of O_2 per hr. to CO.

Substitution of the last two expressions in **(20)** gives:

$$B = 0.375\,LY_1 + 0.750\,L(1 - Y_1) \tag{21}$$

or

$$B = 0.375\,L(2 - Y_1). \tag{22}$$

38. Equation **(22)**, which gives the carbon burning rate in terms of total lb. of O_2 per hr. and fraction of oxygen, on the weight basis, which forms CO_2, is not suitable for convenient use. It would be more desirable to have cu. ft. of air per hr. and percentage of carbon dioxide of total carbon gases (volume basis) than lb. of O_2 per hr. and fraction (weight basis) of oxygen which forms CO_2. The two changes suggested should be made one at a time. First will be found the relation of the fraction of O_2 which forms CO_2 to the volume percentage of CO_2 of the total carbon gases for a substitution in **(22)**. To accomplish this the following scheme is used:

$C +$	O_2	$\longrightarrow$	CO_2
	One molecule	$\longrightarrow$	One molecule
	One lb.-molecule	$\longrightarrow$	One lb.-molecule
	One lb.-molecule	$\longrightarrow$	One lb.-molecular volume
	32 lb.	$\longrightarrow$	359.1 cu. ft. (S.T.P.)
	LY_1 lb.	$\longrightarrow$	Z_1 cu. ft. (S.T.P.)

$$\frac{32}{LY_1} = \frac{359.1}{Z_1} \tag{23}$$

or

$$Z_1 = 11.222\,LY_1. \tag{24}$$

$2C +$	O_2	$\longrightarrow$	$2CO$
	One molecule	$\longrightarrow$	Two molecules
	One lb.-molecule	$\longrightarrow$	Two lb.-molecules
	One lb.-molecule	$\longrightarrow$	Two lb.-molecular volumes
	32 lb.	$\longrightarrow$	2(359.1) cu. ft.
	$L(1 - Y_1)$ lb.	$\longrightarrow$	Z_2 cu. ft.

$$\frac{32}{L(1 - Y_1)} = \frac{2(359.1)}{Z_2} \tag{25}$$

or

$$Z_2 = 22.444\,L(1 - Y_1). \tag{26}$$

Let N_v = percentage (vol.) of CO_2 from coke of total carbon gases from coke, but $\left(\frac{Z_1}{Z_1 + Z_2}\right) 100$ is equal to the same thing, then

$$N_v = \left(\frac{Z_1}{Z_1 + Z_2}\right) 100. \tag{27}$$

Substituting the value of Z_1 from **(24)** and Z_2 from **(26)** into **(27)** gives

$$N_v = \left(\frac{11.222\,LY_1}{11.222\,LY_1 + 22.444\,L(1 - Y_1)}\right) 100, \tag{28}$$

which simplifies to

$$Y_1 = \frac{2N_v}{100 + N_v}. \tag{29}$$

This value of Y_1 may now be substituted in **(22)**, in which case

$$B = 0.375L\left(2 - \frac{2N_v}{100 + N_v}\right) \tag{30}$$

or

$$B = 0.375L\left(\frac{200}{100 + N_v}\right). \tag{31}$$

It now remains to find L (lb. of O_2 per hr.) in terms of cu. ft. of air per hr. As the weight of a gas may be taken as proportional to its volume, it may be said that

$$\textit{lb. of } O_2 \textit{ per hr.} = K_3 \textit{ (cu. ft. of } O_2 \textit{ per hr.)}, \tag{32}$$

where K_3 is a constant equal to the number of lb. of O_2 per cu. ft.

Molecular Weight and Volume Relations

39. K_3 can be evaluated by substituting experimental values in **(32)**, or, more simply, by substituting the gram-molecular weight and gram-molecular volume. To arrive at these values, it is expedient to discuss the relation between the molecule and its

weight and the gram-molecule and its weight. The unit for weighing atoms is 1/16 the weight of an oxygen atom and is usually referred to as the atomic weight unit. Since it is difficult to associate the appropriate volume with such a small quantity, the concept of a gram-molecule, which contains the same large number of molecules for all substances, was developed by chemists many years ago so that a material could be weighed conveniently in grams for molecular weight determinations. The quantity of a substance which weighs the same in grams that its molecule weighs in atomic weight units is a gram-molecule. Likewise, the quantity which weighs the same in pounds is a pound-molecule. A molecule of oxygen (O_2) weighs 32 atomic weight units; a gram-molecule weighs 32 grams; a pound-molecule 32 pounds; a ton-molecule 32 tons. The volume of a pound-molecule is known as the pound-molecular volume.

40. If this pound-molecular volume is determined experimentally for various gases as 32°F. and a pressure of 29.92 inches of mercury, it will be found to be close to 359.1 cu. ft. regardless of what the specific gas may be. This fundamental property of gases led to Avogadro's law that equal volumes of all gases under the same conditions contain the same number of molecules. Owing to certain attractions or repulsions of individual molecules, the law does not hold at high pressures or at very low temperatures (much lower than are found in a foundry). Some gases like carbon dioxide deviate considerably under these extreme conditions and even appreciably under normal atmospheric conditions. Under foundry conditions, the greatest deviation from this ideal gas law is shown by carbon dioxide to be about 0.5 per cent. The pound-molecular weight of a substance can be determined by adding the atomic weights (found in an atomic weight table) which make up the molecular weight. This weight as pounds will occupy 359.1 cu. ft. (S.T.P.) if the substance is a gas. The pound-molecular weight of oxygen is 32 lb., therefore, equation **(32)** becomes

$$32 = K_s\,(359.1)$$

or

$$K_s = 0.08911 \text{ lb. per cu. ft.}$$

$$\text{Let } S = \text{cu. ft. of } O_2 \text{ per hr.},$$

then **(32)** becomes

(33) $$L = 0.08911S.$$

Substituting the value of L from **(33)** in **(31)**,

(34) $$B = 0.375\ (0.08911S)\left(\frac{200}{100 + N_v}\right)$$

or

(35) $$B = 0.03342S\left(\frac{200}{100 + N_v}\right).$$

41. Instead of cu. ft. of O_2 per hr. (S), it would be advantageous to use an equation involving cu. ft. of air per hr. If C_1 is the cu. ft. of air per hour and 20.8 per cent of it is oxygen (volume basis), then

(36) $$S = 0.208C_1.$$

This value of S introduced into **(35)** gives

(37) $$B = 0.03342\ (0.208C_1)\left(\frac{200}{100 + N_v}\right)$$

or

(38) $$B = 0.00695C_1\left(\frac{200}{100 + N_v}\right).$$

42. It may be still more desirable to substitute a value of N_v in terms of percentage of CO_2 in the cupola effluent gas, but to do so to any advantage the amount of limestone must be fixed at some specific value. Now by definition,

(39) $$N_v = \frac{(X - M)100}{X - M + Y}.$$

where X–M is the percentage of CO_2 and Y is the percentage of CO, both from coke. In the case where the limestone is 25 per cent by weight of the carbon of the coke, W is equal to 0.25 and M is related to X and Y as shown in equation **(11)**. The substitution of this value of M in **(39)** gives

(40) $$N_v = \frac{(X-[1.263X + 0.763Y - 26.26])100}{X-[1.263X + 0.763Y - 26.26] + Y}.$$

Simplifying,

$$N_v = \frac{2626 - 26.3X - 76.3Y}{26.26 - 0.263X + 0.237Y}. \quad (41)$$

Introducing the value of Y from **(16)** into **(41)** gives

$$N_v = \frac{2626 - 26.3X - 76.3\ [1.682\ (21.28 - X)]}{26.26 - 0.263X + 0.237\ [1.682\ (21.28 - X)]} \quad (42)$$

or

$$N_v = \left(\frac{1.02X - 1.04}{34.74 - 0.662X}\right) 100. \quad (43)$$

Replacing N_v in **(38)** with its value in **(43)**,

$$B = 0.00695C_1 \left(\frac{200}{100 + \frac{1.02X - 1.04}{34.74 - 0.662X}}\right) \quad (44)$$

or

$$B = (0.0257C_1)\left(\frac{52.48 - X}{94.13 + X}\right). \quad (45)$$

Since

$$C_1 = 60C, \quad (46)$$

where C_1 is the cu. ft. of air per hr. and C is the cu. ft. of air per min., the substitution of the value of C_1 in **(45)** gives

$$B = 1.542C\left(\frac{52.48 - X}{94.13 + X}\right). \quad (47)$$

Conditions Governing Use of Formulae

43. Either **(38)** or **(47)** can be used to calculate the carbon-burning rate in lb. per hr. The volume of air per hr. and the ratio of CO_2 to the total carbon-gases ($N_v/100$) are needed for **(38)**; in **(47)** air-volume per min. and the percentage of CO_2 in the cupola effluent gas are required. Equation **(38)** is of general applicability and can be used in any case where all the oxygen burns to either CO or CO_2; **(47)**, however, can only be employed for the specific case in which the limestone is 25 per cent of the weight of the carbon of the coke.

MELTING RATE

44. From time to time in the operation of an iron foundry, it is desirable to know the melting rate in tons per hr. at a specific time. It is easy enough to obtain the average melting rate for each heat inasmuch as the total iron melted can be divided by the number of hours required to make that heat. Any system of weighing the molten metal, particularly if a holding ladle is used with a continuously tapped cupola, is likely to be inconvenient and also lacking in suitable accuracy. One of the simplest formulas to calculate the melting rate is an expression in which the rate in tons per hr. is obtained by multiplying the carbon disappearance rate in tons per hr. by the iron-carbon ratio. This equation may be represented as

$$D = \frac{B_2 R}{2000}, \tag{48}$$

where D is the melting rate in tons per hr., B_2 is the carbon disappearance rate in lb. per hr., and R is the iron-carbon ratio (lb. of iron charged per lb. of carbon charged).

Derivation of Formula

45. The derivation of equation **(48)** is based on the assumption that the cupola is charged uniformly with all ingredients, including the air, until some type of dynamic equilibrium is attained. Under this condition, 100 charges of iron will melt in the time required for the disappearance of 100 charges of carbon (coke). In other words, the disappearance of carbon allows the iron to enter the melting zone and, at dynamic equilibrium, the rate of entering as a solid must equal the rate of leaving as a liquid. Consequently the melting rate must equal the carbon disappearance rate, in the same weight units, multiplied by the amount of iron charged per pound of carbon.

46. The operation of the cupola under the condition of dynamic equilibrium may be regarded as a kind of chain phenomenon. It is analogous to the situation in which materials are conveyed by a belt. Suppose, in the case of conveying a certain type of casting, that a penny were to be placed on the belt after each seventeenth casting and that all pennies would fall into a box where the castings leave the belt. Multiplying the number of pennies by 17 would give the number of castings as accurately as would unit counting. If, at some specific time, 17 pennies were found in the box, 289

castings would have been conveyed. Likewise, in the case of the operation of the cupola, the rate of passage of carbon (carbon disappearance rate) may be regarded as an accurate measure of the rate of iron passage (melting rate).

Equations for Carbon Disappearance

47. There are two ways in which the carbon (coke) disappears from the cupola that will account for practically all carbon (coke) that is charged. It either burns forming gases that leave with the cupola effluent gas or dissolves in the molten iron. If B_1 represents the carbon dissolving rate in lb. per hr., B (burning rate) $+ B_1$ will equal B_2 (carbon disappearance rate) or, mathematically,

$$B + B_1 = B_2. \tag{49}$$

Substitution of this value of B_2 in **(48)** gives

$$D = \frac{(B + B_1)R}{2000}. \tag{50}$$

Neither B nor B_1 can be measured conveniently by direct means; therefore, it is advisable to find a mathematical relation of these quantities to something that can be satisfactorily measured. Equation **(47)** gives B in terms of percentage of CO_2 (X) and air-volume (C). It remains to find a similar relation for B_1.

48. Aside from air-volume per min. and melting rate, other factors connected with the cupola operation are usually expressed as amounts rather than as rates. Carbon dissolved would be regarded as the amount dissolved in a certain amount of iron instead of the amount dissolved in a unit time. If the time is known, this amount (A_1) can be divided by time (t_1) to give the carbon dissolving rate (B_1). In other words,

$$B_1 = A_1/t_1 \tag{51}$$

or

$$A_1 = B_1 t_1. \tag{52}$$

The carbon burning rate (B) bears the same relation to the amount of carbon burned (A), which may be given as

$$A = Bt. \tag{53}$$

Equation **(52)** divided by **(53)** will yield

$$\frac{A_1}{A} = \frac{B_1 t_1}{Bt}. \tag{54}$$

49. If both A_1 and A are measured for an identical time period, t_1 is equal to t and

$$\frac{A_1}{A} = \frac{B_1}{B}. \tag{55}$$

A solution of **(55)** for B_1 results in

$$B_1 = \left(\frac{A_1}{A}\right)\mathrm{B}. \tag{56}$$

If the ratio $\frac{A_1}{A}$, for convenience, is designated as r,

$$B_1 = rB. \tag{57}$$

50. It is not to be construed that B_1 is a function of B; the two quantities are absolutely independent of each other. Actually B can change while B_1 remains constant, but an appropriate change must take place in r so that the identity will be maintained.

Typical Calculation of Melting Rate

51. A problem will now be used to demonstrate how r can be determined. The type of charge is given in Table 2. The average

Table 2

CHARGE ON WHICH CALCULATION IS BASED

Material	*Weight (lb.)*	*Per Cent Carbon*	*Weight × Per Cent C*
Pig iron	800	4.00	3200
Foundry returns	1200	3.25	3900
Cast-iron scrap	800	3.10	2480
Steel scrap	1200	0.50	600
Total	4000		10180

carbon of the metal composite going into the cupola may be obtained by dividing 10180 by 4000. This value is 2.545 per cent. Because 3.25 has been selected as the carbon content of the foundry returns, it is advisable to assume that all heats made with the above charge will yield iron containing 3.25 per cent carbon under

this special condition of operation. This value is based upon the weight of the original iron charge plus the weight of the carbon which has been dissolved. If A_1 is taken as the weight of carbon in pounds that dissolves in each charge, the weight of metal obtained from 4000 lb. will be $(4000 + A_1)$ lb. The two percentage-values are on two different bases. The value 2.545 is based on a total of 4000 lb. of metal, while the value 3.25 is based on $(4000 + A_1)$ lb. The amount of carbon in the iron charged may be obtained by multiplying 4000 by 2.545/100; the amount in the final iron by multiplying $(4000 + A_1)$ by 3.25/100. The amount of carbon dissolved per charge will be the difference in the two products given above. Thus,

(58) $$A_1 = 0.0325\ (4000 + A_1) - 0.02545\ (4000).$$

from which

$$A_1 = 29.15\ lb.$$

52. If the coke charge corresponding to the 4000-lb. iron charge is such that it contains 400 lb. of carbon, the amount of carbon burned per charge will be

$$A = 400 - 29.15 = 370.85\ lb.$$

It follows, inasmuch as the weight dissolved and the weight burned correspond to the same time period, that

(59) $$r = \frac{A_1}{A} = \frac{29.15}{370.85} = 0.0786.$$

53. Because r can be determined with suitable convenience, the further expansion of these considerations will be made with free use of this symbol in the mathematical equations. A substitution of the value of B_1 from **(57)** in **(50)** gives

(60) $$D = \frac{(B + rB)R}{2000}$$

or

(61) $$D = \frac{(1 + r)\ BR}{2000}.$$

Substituting the value of B from **(47)** in **(61)**

$$D = \frac{1.542\ RC\ (1 + r)\left(\frac{52.48 - X}{94.13 + X}\right)}{2000}, \tag{62}$$

which may also be written as

$$D = 7.71 \times 10^{-4}\ RC\ (1 + r)\left(\frac{52.48 - X}{94.13 + X}\right). \tag{63}$$

For the particular case where r is 0.0786 [see the development of **(59)**] equation **(63)** may be written as

$$D = 8.32 \times 10^{-4}\ RC\left(\frac{52.48 - X}{94.13 + X}\right). \tag{64}$$

54. The melting rate as given by D is the rate at which the metal charge melts and it is not the rate of obtaining molten metal from the cupola spout. The rate of obtaining metal, as a matter of differentiation, will hereinafter be designated by D_1 given in tons per hr. To get a precise value D_1 from D, several corrections should be made. The metal charge should be corrected to metal free of rust and proper allowance given to the amount of carbon dissolved and to the amount of any alloying elements added in the cupola. Other corrections, such as loss of silicon, are probably of minor importance. Actually, if fairly large pieces of scrap are used, the error introduced by neglecting the rust will not be of significant magnitude.

55. There is left only the correction for carbon pick-up, which may be made by using the carbon dissolving rate, $B_1/2000$. Because the weight of carbon dissolved must be added to the iron of the original metal to get the weight of metal obtained at the spout, it may be said that

$$D_1 = D + B_1/2000. \tag{65}$$

Substituting the value of D from **(60)** and B_1 from **(57)**,

$$D_1 = \frac{(B + rB)R}{2000} + \frac{rB}{2000}. \tag{66}$$

On collecting terms,

$$D_1 = \frac{B\ (R + r\ R + r)}{2000}. \tag{67}$$

The introduction of B from **(47)** results in

$$(68) \qquad D_1 = 7.71 \times 10^{-4} C(R + r\,R + r)\left(\frac{52.48 - X}{94.13 + X}\right)$$

Cupolas Operating with no Steel in Charge

56. Many cupolas, operating with no steel in the metal charge, effect the dissolution of very little, if any, carbon. In this special case, both B_1 and r become equal to zero and D is coincident with D_1. Furthermore, **(68)** becomes

$$(69) \qquad D_1 = 7.71 \times 10^{-4} RC\left(\frac{52.48 - X}{94.13 + X}\right)$$

and **(63)** becomes

$$(70) \qquad D = 7.71 \times 10^{-4}\, RC\left(\frac{52.48 - X}{94.13 + X}\right).$$

Basic Conditions for Mathematical Relations

57. It should be re-emphasized that the mathematical relations which have just been evolved are attendant upon the assumption that all the iron associated with one pound of carbon melts in the same length of time, on the average, that is required for the disappearance of one pound of carbon. This condition obtains and the equations are applicable only where the weight of each constituent remains constant in the charge until a sort of equilibrium is attained in the cupola. This "operational equilibrium," even though it is dynamic, should not be confused with the thermodynamic equilibrium of a chemical reaction. Once thermodynamic equilibrium is reached within a closed system, no net change will ever take place until something is either introduced or withdrawn from the system. It is not affected by the kinetics of the reaction except that the point of equilibrium is realized in a different length of time. The operational equilibrium, on the other hand, depends upon the kinetics of the chemical reactions and the rates of introducing the substances into the cupola.

58. Another distinction is that in operational equilibrium there can be no such thing as a closed system, a condition usually necessary for thermodynamic equilibrium. Some materials must obviously go in and others come out or the cupola is not performing as a melting furnace.

59. This condition of operational equilibrium does not require a fixed point for any variable, but rather a well defined range which may be illustrated by the coke bed height where a 6-in. layer of coke is used in a single charge. The height of the bed reaches the lower limit, which under the required conditions of equilibrium is virtually always the same, just before the new 6-in. layer comes in contact with it to raise the height 6 in. to the maximum, which is also virtually the same every time a new layer of coke joins the bed coke. Then it may be said that the equilibrium coke bed height for a particular set of conditions is some average *(H)* with a fluctuation of 6 in., or with an average deviation from the average of 3 in. All other variables will fluctuate in correspondence with this change in coke bed height and, therefore, should be measured through a period or group of periods so as to give a representative average.

Comments on Other Melting Rate Formulae

60. Massari and Lindsay[3], in reviewing the work of Buzek[4] and Jungbluth and Korschan[5], give

$$S = \frac{600W}{LKk}, \tag{71}$$

wherein S is the melting rate in metric tons per hour, W is cubic meters of air at 0°C. and 760 mm. pressure, L is the cubic meters of air required per kilogram of carbon, K is the quantity of coke in kilograms used for melting 100 kilograms of iron and k is the percentage of carbon in the coke. This equation is substantially the same as **(70)**, except it is more difficult to use. L in itself is not easy to measure, and therefore the usefulness is improved where the value of L in terms of N_c is substituted, N_c being the percentage of CO_2 in the total carbon gases from the coke.

61. Reese[6], on the other hand, gives an equation which differs considerably from **(70)** and **(71)** under certain conditions. This equation,

$$\frac{\textit{Fuel burning rate (in lb.)} \times \textit{fuel ratio} \times \tfrac{1}{4}\sqrt{P}}{2000} = \textit{melting rate (tons per hr.)}, \tag{72}$$

may be expressed as:

$$D = \frac{1/4\ BR\ \sqrt{P}}{2000}, \tag{73}$$

where P is the pressure of the blast in ounces, and the other variables conform to the convention of this paper. In the special case where P is 16 oz., $1/4\ \sqrt{P}$ becomes $1/4\ \sqrt{16}$ or 1 and has no effect for it is used as a multiplier. Where P is 9 oz., $1/4\ \sqrt{P}$ is equal to 3/4 and therefore the calculated melting rate by **(73)** will be only 3/4 as much as by **(48)**. The pressure, of course, has an effect but this effect is hidden in B and does not need to be considered separately.

Checking Air-measuring Instruments Mathematically

62. Equations **(68)**, **(69)**, and **(70)** can be used to calculate the air-volume *(C)* as a check on the performance of the air measuring instruments. The most important use is that of determining the melting rate for some definite time period. In some cases, the number of foundry rejects may be quite sensitive to a change in melting rate. Faster melting may increase, decrease, or have no effect upon the loss, depending upon the type of casting and the manner in which it is made. In the sensitive cases, it is not only important to know the average melting rate for the heat, but also to know the melting rate for short intervals at different times throughout the heat. Even one-half hour at a melting rate different from the optimum by as much as 2 tons per hr. may materially affect the percentage of rejects for the entire heat.

63. In the special case where r is zero, equation **(70)** can be used to determine the melting rate and hence the uniformity of the melting rate throughout the heat. As to the use of the equation in determining the air-volume, attention is called to the fact that the volume calculated is not that recorded or measured by the wind instruments, but rather the volume which actually goes through the cupola. To get this volume correctly the iron-carbon ratio should be held constant throughout the heat, the CO_2 content of cupola effluent determined every 15 to 30 min., and the average melting rate found for the heat by dividing the weight of metal melted by the number of hours required to make the heat. The substitution of these values in **(70)** will give the average volume of air per minute used throughout the heat. Obviously the volume regulation should be made so as to hold the amount of air per minute as constant as possible.

THE A- AND Z-FAMILIES OF VARIABLES

64. In equation **(68)**, there is a mathematical relation among melting rate in tons per hr., iron-carbon ratio, volume of air per

min. in cu. ft. (S.T.P.), the percentage of CO_2 in the cupola effluent, and the ratio of the carbon dissolved in melting to that burned. These factors are most important, but there are many more that are also essential. Among the other significant variables may be listed the type of fuel, size and reactivity of the fuel, temperature of the blast, size and height of the cupola, composition of the metal, and the temperature of the metal. The variables in **(68)** may be considered in a closed system, which, regardless of what happens outside, will adjust themselves so that the mathematical relation holds.

65. In order that the two sets of variables may be differentiated with ease, those occurring in equation **(68)** have been given the name of the "A-family" of variables pertaining to cupola operation, while all others have been grouped into the "Z-family." Equation **(68)** will then be referred to as the "general equation of A-variables." For the case where no carbon is dissolved during melting, r becomes zero and the general equation takes the form of **(69)** or **(70)** which are really identical. In order that subsequent discussion be as simple as possible, reference will be made only to this simplified form of the equation.

66. The development of the simplified equation has in no way been directly related to or dependent upon the Z-family of variables so that the relation is one which may be considered to represent a mathematical truth regardless of what variations may take place in the Z-family. The set of A-variables may be considered as self-contained so then a change in one, because of some change in the Z-family, must be accompanied by a change in at least one other. For the special case where the iron-carbon ratio is held at 7 and the air-volume is maintained at 7500 cu. ft., **(70)** becomes

$$\textbf{(74)} \qquad D = 40.48 \left(\frac{52.48 - X}{94.13 + X} \right).$$

67. The graphical relation is shown in Fig. 1. It is obvious that the melting rate increases as the CO_2 content decreases, regardless of what specific changes may take place in the Z-family. Actually, however, D cannot vary where R and C are constant unless some variation takes place in the Z-family, because X is a function of R and hence must be constant where R is constant, where the Z-variables are also constant. It follows from the "equation of A-variables" that D must be constant if R, C, and X are.

It may be said that, if R and C are constant and there is no variation in the Z-family, the "equation of A-variables" becomes inoperative as a line and for this special condition a plot of X and D will give only a point. Variations within the Z-family, while R and C are constant, will give different points, but they all must be on the line represented by equation **(74)**. Under such conditions if the melting rate is changed from, say, 15 to 16 tons per hr. by changing one variable of the Z-family, a certain decrease will result in CO_2 concentration. If the same effect on melting can be produced by the changing of some other variable, exactly the same decrease will result in the CO_2 concentration.

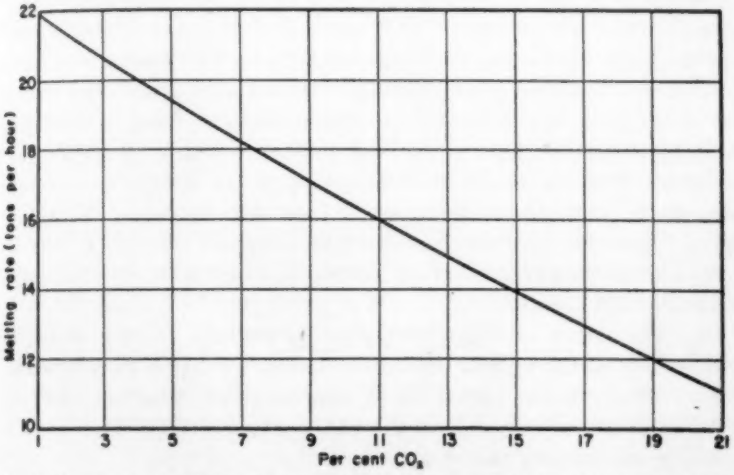

FIG. 1—VARIATIONS IN MELTING RATE WITH CHANGES IN PER CENT CO_2 (FROM EQUATION OF A-VARIABLES IF R IS 7.0 AND C IS 7500).

Effect of Varying Coke Size Only

68. To illustrate, consideration may be given to the variation in coke size (Z-variable) where all other independent variables of both families are held constant. Smaller coke, having a larger surface for the same weight of coke than larger pieces, will favor the complete disappearance of oxygen closer to the tuyeres, and therefore will give a greater opportunity for the formation of carbon monoxide. Smaller coke then would cause the formation of a higher percentage of CO (lower CO_2) which would be accompanied by faster melting. Where R is constant, this fact means that less heat is produced for a given amount of metal and therefore the smaller pieces of coke will yield colder iron than will the larger pieces, provided, of course, the pieces are not so large that the

oxygen is not totally consumed in the combustion zone. Such a consideration of fuel size becomes imperative if a comparison of fuels is to be made.

69. Furthermore, equation **(74)** may be used to throw light upon such a controversial subject as whether a hot blast increases or decreases the CO_2 content in the cupola effluent. It has been pointed out by some specialists that a hot blast (600°F.) will increase the melting rate, and, at the same time, it has also been said that the hot blast causes an increase in the percentage of CO_2 in the cupola effluent. The temperature of the blast is a Z-variable; and if the ratio *(R)* and air-volume *(C)* are constant, and if the hot blast under these conditions increases the melting rate, it follows that the percentage of CO_2 must be lower, not higher, than where a cold blast is used. The lower CO_2 content where R is constant means less heat is produced per pound of metal in the burning of the fuel, but this effect is compensated, at least in part, by the higher sensible heat in the hot blast. Whether the iron will be hotter with the hot blast depends upon the magnitude of the two effects. The impression gained from the literature is that a 600°F. blast will probably give an iron from 50° to 100°F. hotter than a room temperature blast where the other independent variables are held constant.

70. The curve in Fig. 1 deviates so slightly from a straight line that in actual cupola operation it may be taken as a straight line in the particular region which applies. For instance, where R is 7 in an ordinary operation, the CO_2 content will be close to 11.5 and it is satisfactory to use

$$D = 21.5 - 0.51X, \tag{75}$$

but it should be realized that this form is only an approximation of the exact equation and can be used only within a reasonable range. It is realized that there are many opportunities to employ simple equations as sufficiently accurate approximations under the proper conditions; but to avoid the chance of introducing errors which might be accumulative, aside from **(75)**, only the more exact equations are being considered at length in this paper.

Effect of Varying Melting Rate and Iron-Carbon Ratio

71. It is interesting to consider equation **(70)** with all other pairs of variables constant. The situation in which R and C are constant has just been discussed. Let us consider the case in which

air-volume *(C)* and per cent CO_2 *(X)* are constant and melting rate *(D)* and iron-carbon ratio *(R)* are allowed to vary. As *C* is an independent variable, it can be controlled directly at will, but *X* is dependent and it will be necessary to make changes in a *Z*-variable to hold *X* constant, while *D* is allowed to vary only because of changes in *R*. In the case at hand, the melting rate *(D)* will be directly proportional to the iron-carbon ratio *(R)*. In other words, where the iron-carbon ratio is doubled the melting rate will also be doubled. On the contrary, if there is no variation in the Z-family, *X* will increase with *R* and doubling the iron-carbon ratio will not quite double the melting rate.

72. Jungbluth and Korschan[5] found that, for a given type and size of coke used in a cold blast cupola, the variation in percentage of CO_2 depends entirely upon the iron-carbon ratio *(R)*, not being affected by reasonable changes in air-volume. Piwowarsky and Meyer[7] make the same contention. Inasmuch as percentage of CO_2 *(X)* depends on only the iron-carbon ratio *(R)* of the A-family, the melting rate *(D)* is directly proportional to the air-volume where *X* and *R* are constant with no help at all from the Z-family. Then, where *X*, *R*, and the independent *Z*-variables are constant, a doubling of the air-volume results in a melting rate which is also twice as great. A variation in the Z-family may make it impossible to hold *X* constant where *R* is constant.

73. Where the melting rate *(D)* and percentage of CO_2 *(X)* are held constant with the help of the Z-family, it will be necessary for the iron-carbon ratio to be proportional to the reciprocal of the air-volume. Where the iron-carbon ratio is doubled, the air-volume necessary to maintain constant melting rate and percentage of CO_2 will be half as much.

74. In the consideration of the case where the melting rate *(D)* and air-volume *(C)* are to be held constant, *D* can only be held constant, if *R* varies, by appropriate variations in the Z-family. *C* is independent and can be controlled at will. Where *D* and *C* are thus held constant, *R* is a function of *X* such that *X* increases as *R* increases but not linearly. On the contrary, if the *Z*-variables are held constant, N_s, which is directly related to *X*, is a quadratic function of $1/R$ with an accompanying change in melting rate.

75. In holding *D* and *R* constant, again it is necessary to call on the Z-family for help if both *C* and *X* are to vary. In this case, an increase in air-volume will give an increase in CO_2. This is exactly what would be expected because faster moving air would be depleted of oxygen farther from the tuyeres where there is not

as much coke bed left for the formation of CO.

SUMMARY OF MOST USEFUL EQUATIONS

76. In presenting a summary of the most useful equations, it is fitting to give first a list of all the variables involved:

X = per cent CO_2 in cupola effluent,
Y = per cent CO in cupola effluent,
Z = per cent O_2 in air,
N_c = per cent CO_2 in total carbon gases (all from coke),
M = per cent CO_2 (effluent basis) from limestone,
W = weight ratio of limestone to carbon of coke,
B = carbon burning rate in lb. per hr.,
B_1 = carbon dissolving rate in lb. per hr.,
B_2 = carbon disappearance rate in lb. per hr.,
A_1 = amount of carbon dissolved in lb.,
A = amount of carbon burned in lb.,
t_1 = time (hr.) corresponding to A_1,
t = time (hr.) corresponding to A,
$r = A_1/A$,
C_1 = cu. ft. of air per hr. (0°C. 760 mm., 32°F. 29.92-in.),
C = cu. ft. of air per min. (0°C. 760 mm., 32°F. 29.92-in.),
R = iron-carbon ratio,
D = melting rate of iron in the metal charge (tons per hr.), and
D_1 = melting rate (rate of obtaining metal in tons per hr.).

77. For all the equations developed, a type of dynamic equilibrium has been assumed. A further assumption has been made that there are considerable fluctuations arising from layer charging; therefore, the values should be taken as an average over an exact whole number of periods in so far as can be determined. If 4000 lb. of metal is charged at one time and the melting rate is 16 tons per hr., then ⅛ hr. or 7-½ min. would be considered one period. CO_2 measurements should be averaged for 7-½, 15, or some other multiple of 7-½ min. The most useful equations and their limitations are shown in Table 3.

Equation Limitations

78. The equations of Table 3 are not valid under all conditions and therefore it is advisable to list the limitations imposed upon each:

1. Practically all carbon of the coke must be dissolved in the

Table 3

MOST USEFUL EQUATIONS AND THEIR APPLICATIONS

Number	*Equation*	*Use*
(7)	$Y = \left(\frac{200}{100 + Z}\right)(Z - X)$	General equation for CO
(9)	$Y = 1.656\,(20.8 - X)$	For CO when $Z = 20.8$
(11)	$M = 1.263X + 0.763Y - 26.26$	To determine CO_2 from limestone
(13)	$M = \frac{W(X + Y)}{W + 8.33}$	To determine CO_2 from limestone
(16)	$Y = 1.682(21.28 - X)$	For CO where $W = 0.25$
(38)	$B = 0.00695C_1\left(\frac{200}{100 + N_s}\right)$	To determine carbon-burning rate
(39)	$N_s = \left(\frac{(X - M)100}{X - M + Y}\right)$	To evaluate N_s
(45)	$B = 0.0257C_1\left(\frac{52.48 - X}{94.13 + X}\right)$	To determine carbon-burning rate
(47)	$B = 1.542C\left(\frac{52.48 - X}{94.13 + X}\right)$	To determine carbon-burning rate
(48)	$D = \frac{B_2R}{2000}$	To determine melting rate
(68)	$D_1 = 7.71 \times 10^{-4}C(R + r\,R + r)\left(\frac{52.48 - X}{94.13 + X}\right)$	"General equation of A-variables." To determine melting rate or air-volume
(70)	$D = 7.71 \times 10^{-4}RC\left(\frac{52.48 - X}{94.13 + X}\right)$	"Simplified equation of A-variables." To determine melting rate or air-volume
(74)	$D = 40.48\left(\frac{52.48 - X}{94.13 + X}\right)$	Special equation to estimate the effect of Z-variables where R and C are constant

iron or burned to CO and CO_2, a condition necessary for all equations.

2. The cupola effluent is composed of CO, CO_2, and the inert gases (considered as nitrogen) from the atmosphere and nothing more in appreciable quantity—applies to all equations.
3. Equations **(7)** and **(9)** cannot be applied where limestone is used.
4. Equations **(16)**, **(45)**, **(47)**, **(69)**, **(70)**, and **(74)** apply only where the limestone is 25 per cent ($W = 0.25$) by weight of the total carbon.
5. The cupola must be operating at dynamic equilibrium before **(38)**, **(45)**, **(47)**, **(48)**, **(69)**, **(70)** and **(74)** can be applied. That is, the charging must be held constant for possibly 30 min.
6. The percentage of CO_2 from coke $(X - M)$ cannot appreciably exceed 20.8 per cent; the iron-carbon ratio (R) must be sufficiently small that the melting proceeds continuously; the air-volume (C) must be small enough that practically all the oxygen reacts with carbon.

Conclusions and Summary

79. From the previous discussion, the following conclusions can be drawn:

1. In the burning of carbon, CO is formed stepwise through the intermediate product, CO_2.
2. The correct mechanism of fuel reactions is not needed to make a heat balance, but is valuable in indicating what changes should be made in operation to get a desired heat balance.
3. A hot cupola tends to form more CO.
4. The carbon monoxide content of the cupola effluent can be calculated from the CO_2 content and the amount of limestone used.
5. According to Belden's data, the oxygen content of the air may be used as 20.8 per cent (by volume) in equations pertaining to the cupola, if the analyses are done by absorption in liquids.
6. The specific conditions, under which the various equations may be applied, have been pointed out.
7. Melting rate in tons per hr., iron-carbon ratio, air-volume (cu. ft. S.T.P.), percentage of CO_2 in the cupola effluent,

and the ratio of carbon dissolved to that burned have been classed as the A–family of fundamental variables pertaining to the cupola; all other variables have been referred to as being in the Z–family.

8. The equation

$$D = 7.71 \times 10^{-4} RC \left(\frac{52.48 - X}{94.13 + X} \right)$$

has been called the "simplified equation of A-variables."

9. There is a different "equation of A-variables" for each different amount of limestone used (reflected in W).

10. A variation of a single variable in the Z–family means that at least two, if any, must vary in the A–family.

11. If the hot blast, with other independent variables constant, increases the melting rate, it must at the same time lower the percentage of CO_2 in the cupola effluent.

12. The special "equation of A–variables" where the iron-carbon ratio *(R)* and air-volume *(C)* are held constant represents only a point if the independent variables of the Z-family are also constant; a variation of a *Z*-variable will cause the point to move on the line

$$D = 40.48 \left(\frac{52.48 - X}{94.13 + X} \right).$$

13. In comparing different fuels, size should be taken into consideration. It is reasonable to expect more CO and hence lower iron temperature where the coke is small.

14. As long as the iron-carbon ratio and temperature of the blast are constant, an increase in CO indicates colder iron; a decrease in iron-carbon ratio will give more CO and hotter iron; hotter blast will give more CO and hotter iron.

ACKNOWLEDGMENT

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DISCUSSION

Presiding: F. J. WALLS, International Nickel Co., Detroit, Mich.
Co-Chairman: E. E. MARBAKER, Mellon Institute, Pittsburgh, Pa.

F. K. VIAL* *(written discussion):* In the development of the paper, the author limits his mathematical analysis to a few generalities relating to combustion as developed in producer gas furnaces, blast furnaces, and coal or coke combustion furnaces in general, to the exclusion of the large number of characteristic fundamentals which differentiate the cupola from other types of furnaces.

After a laborious mathematical development, two principal equations are set forth:

(47) B = rate of burning = $1.542C\left(\frac{52.48 - X}{94.13 + X}\right)$;

(70) D = rate of melting = $7.71 \times 10^{-4} RC\left(\frac{52.48 - X}{94.13 + X}\right)$.

These equations are unsolvable in themselves. However, both items are always known in every heat by the record of the iron-to-coke ratio as charged and the time required to melt one ton of iron.

Throughout the paper many vital conclusions are drawn independently of any mathematical treatment, many of which seem to be in direct opposition to experience, and would be decidedly disastrous if applied generally to cupola practice.

Composition of Air

For the determination of the percentage of oxygen in the air, two and one-half pages containing 75 gas analyses are recorded and the oxygen contained calculated and compared with the nitrogen content of the gases, by means of which it was found that the air contains 20.8 per cent of oxygen instead of 20.9 per cent, the universally accepted percentage.

It would seem that the difference could be accounted for in variations in chemical analysis which would indicate that the average of the 75 analyses of gas samples was in close agreement with the actual 20.9 per cent. However, the author rejects the 20.9 per cent and accepts the 20.8 per cent as the desirable percentage to use throughout the calculations.

This difference in no way affects the results and, therefore, seems to have received unwarranted space in its development.

Ratio of CO (Y) to CO_2(X)

(9) $$Y = 1.656\ (20.8 - X)$$

In establishing this ratio, the author indulges in an excess of algebraic regalia to such an extent that the constants with which we are so well acquainted become disguised and are unrecognizable. Formulae (5), (6), (7), (8) and (9) are used to develop 1.654 as the ratio of the volumes of CO_2 and CO.

The familiar method of determining this ratio is the following:

The per cent of CO_2 when CO is zero is 20.89 per cent
The per cent of CO when CO_2 is zero is 34.71 per cent

*Griffin Wheel Co., Chicago.

The ratio then is

$$\frac{34.71}{20.89} = 1.661$$

The formula then becomes

$$Y = 1.661\ (20.89 - X)$$

It is difficult to see any improvement to be gained by the use of four or five algebraic equations. Plain arithmetic, where the quantities involved can be visualized, seems to be decidedly preferable. There is no difference in the final result by either formulae, except that 1⅔ is a more convenient factor than 1.656.

CO_2 in Limestone

Many pages are used in the development of equations to account for the CO_2 liberated from limestone. Here again the formulae (10) to (16) inclusive are unwieldy in their application when compared with the simple method of determining the volume of CO_2 from limestone in relation to each lb. of coke and deducting this percentage directly without involved algebraic equations.

Increased Weight of Iron on Account of Carbon Absorption

Another insignificant item which complicates the formulae for general foundry practice is the carbon absorbed by the iron and the consequent increase in the weight of the iron melted.

This might be taken into consideration as far as fuel requirements are concerned when melting steel, but otherwise its consideration is unsound when neglecting the loss by oxidation and impurities weighed into the charge as iron, which is a variable quantity depending upon the type of iron melted. For heavy cast scrap and pig iron, the loss is 2 per cent or less; for miscellaneous scrap of light sections, including miscellaneous malleable iron, the loss may be 4 per cent. The loss, when melting stove plate or heavy planer chips, may reach 8 or 10 per cent; for borings, the loss may exceed 20 per cent, thus entirely overshadowing any slight increase arising from carbon absorption. In the case of pig iron, there is usually a loss of carbon which, if it is taken into account, must be added to the other losses mentioned.

Altogether, the item of carbon absorption is too infinitesimal to be considered.

The space given to the consideration of these insignificant items is entirely out of proportion to their importance. There is no such thing as mathematical exactness in their application. The mathematical solutions of cupola problems are at best only an approximation. The coke and iron are not usually weighed with mathematical precision. The composition of the coke varies somewhat from charge to charge, and the gas samples taken near the lining are not the same as those passing through the center of the charges.

Aside from this, there is a continuous variation in the development of CO_2 to synchronize the time of melting a charge of iron to correspond exactly with the time of burning a charge of coke; hence difficulty in obtaining an average gas sample for analysis.

Some variation exists in the temperature of the effluent gases, whether near the lining or at the center of the charge, and to add to the difficulty, the velocity of the gases near the lining is greater than through the center of the cupola where the resistance is greater; hence a balanced average temperature, which is a very important item in developing heat balances, becomes partly a matter of judgment.

Mathematical conclusions can be no more exact than the data on which the calculations are based.

Considering formulae **(17)** *to* **(47)** *inclusive*

$$B = 1.542C \left(\frac{52.48 - X}{94.13 + X}\right), \tag{47}$$

where B = carbon burning rate in lb. per hr., C = cu. ft. air per min., and X = per cent CO_2.

In the development of the formulae relating to the rate of burning, the author does not follow the fundamentals contained in his conclusion 1, paragraph 79, in connection with formulae (4) ($C + CO_2 = 2CO$) in which one-half the carbon in the CO is taken directly from the coke without burning; hence, in order to be consistent, formula (17) should be

$$B = \text{lb. C in } CO_2 \text{ in effluent gases} + \tfrac{1}{2} \text{ (lb. C. in CO)}$$

According to this conception, the rate of burning and the rate of carbon disappearance are not synonymous and should not be confused. This may best be illustrated by dividing the rate of carbon disappearance into its component parts:

(1) That portion of the carbon which is burned to CO_2 and which develops heat at the rate of 14,550 B.t.u. for each lb. of carbon burned.

(2) That portion which is used to reduce CO_2 to CO, which is heat consuming at the rate of 5850 B.t.u. for each lb. of carbon so used. This portion is not burned.

(3) That portion that is absorbed by the iron. This amount is insignificant in ordinary foundry melting.

A very considerable proportion of the coke is intercepted before reaching the combustion zone, and is not burned at all; and hence all of the author's formulae relating to the rate of burning are widely at variance with the conditions existing within the cupola. This may be illustrated by the data in Table 4.

It is clear that when the volume of air is constant, the rate of burning is always constant, regardless of the coke ratio or percentage of CO_2 in effluent gases.

The percentage of CO_2 is constant at 20.8 per cent regardless of coke ratios or air volumes. The author's formula then becomes

$$B = 1.542\ C \left(\frac{52.48 - 20.8}{94.13 + 20.8}\right) = 0.42\ C$$

Table 4

CARBON BURNING RATE

Per Cent CO_2	*Per Cent Carbon Burned to* CO_2	*Per Cent Carbon Used in Reducing* CO_2	*B.t.u. Developed*	*B.t.u. Lost*	*Net B.t.u.*
0	50	50	7275	2925	4350
8*	65	35	9457	2047	7410
10*	69	31	10,039	1813	8226
12*	73	27	10,621	1579	9042
14*	76	24	11,058	1402	9656
21	100	0	14,550	0	14,550

If we remove the unwieldy mathematics we discover beneath our well-known friends; thus

C = 1 cu. ft. air per min. = 60 cu. ft. per hr.

1 cu. ft. air contains 0.0187 lb. oxygen—60 cu. ft. contains 1.122 lb. oxygen

2.66 lb. oxygen are required to burn 1 lb. carbon, hence 1 cu. ft. air per min. will burn 1.122 divided by 2.66 = 0.42 lb. carbon per hr.

The 29 intermediate formulae are unnecessary.

The percentage of coke that reaches the combustion zone is

$$\frac{\text{per cent } CO_2 \times 10^{-2} + 1}{2 - (4 \text{ per cent } CO_2 \times 10^{-2})};$$

hence the time of burning is always known.

The next step is to consider the conditions which regulate the rate at which carbon is used to reduce CO_2 to CO, with its accompanying heat loss. This is the most important part of the entire analysis.

The author passes over these variables with the remark that they are "hidden in the rate of burning and need not to be considered separately."

This is begging the question, for without considering all the factors, some of which are in an inverse ratio to others, the relation between the rate of burning, rate of carbon disappearance, the B.t.u. produced and the B.t.u. lost in CO_2 reduction is indeterminate; and hence the quantity cannot properly be substituted indiscriminately in algebraic formulae where variable quantities of heat, air velocities and coke ratios are involved.

Rate of Coke Disappearance

In the discussion of coke disappearance, we would point out that

B is a variable dependent upon CO_2 percentage, velocity of the blast and area of the cupola.

B increases as CO_2 decreases, which causes R to decrease.

B increases when velocity of blast increases, which causes R to increase.

B increases when area of the cupola increases; R remains constant.

*Range of cupola practice.

Hence R may vary inversely, or directly, or remain constant as the rate of burning.

Since the relation of R to B is variable, and no equations are developed to show the varying relationships, the equation is unsolvable. It is a fundamental law of mathematics that a single equation cannot solve for two unknown quantities, neither can two equations solve for three unknown quantities. Inasmuch as these variables have been substituted in the various formulae (17) to (70) they are limited and indeterminate and require two values to be substituted at the same time having the proper relationship to each other for any condition that is being analyzed, and these values must be obtained from some outside source; and if the answer is to be obtained elsewhere there is no advantage in substituting them in a formula. None of the equations are solvable for the unknown quantities, and hence have no apparent application in cupola practice.

There are two steps involved in developing the melting rate:

(1) When the thermal relations which fix the limitations of the iron-coke ratio are disregarded. In this case, the rate of melting can be calculated for any combination of iron to coke ratios and variable air volumes.

(2) To determine the limiting coke to iron ratios, it is necessary to construct a heat balance and locate the bottleneck which is the determining factor. The author of the paper is concerned only with the first step in the problem and disregards all thermal relationships.

It would seem that the 23 formulae relating to the rate of melting when the iron ratio is known, and especially when the final equation fails to solve the unknown quantity, are unwarranted when two simple simultaneous equations can be written that permit the solution of the unknown quantity for any combination of coke ratios and air volumes.

The rate of burning has already been shown to be $B = 0.42C$ when C = cu. ft. of air per min. per sq. ft. of cupola area;

Then,

$$D = 0.42C \times \left(\frac{R}{\text{per cent of coke burned in combustion zone}} \right)$$

$$\text{(1) Per cent of coke burned} = \frac{CO_2 \times 10^{-3} + 1}{2 - (4CO_2 \times 10^{-3})}$$

$$\text{(2) Per cent } CO_2 = R + 0.01C.$$

By substituting the value of CO_2 in (1) by its value in (2), all problems relating to the rate of burning or the rate of melting for any value of R are easily solved. The most important part of the problem is establishing the limiting iron-coke ratio for any air volume.

The author passes over this most important of all cupola problems by conclusion 2, paragraph 79: "The correct mechanism of fuel reactions is not needed to make a heat balance, but is valuable in indicating what changes should be made in operation to get a desired heat balance."

The author seems to be satisfied in assuming that the only objective in cupola practice is to burn the coke, which insures that the iron will come down.

As to the ratio of CO_2 to CO, it is almost axiomatic that the greater the percentage of CO_2 in the effluent gases, the hotter the furnace and, consequently, the hotter the iron as melted. If it is so desired, the temperature of the iron may be held stationary while the quantity melted is increased. Conversely, the hotter the cupola, the lower the percentage of CO and the rate of melting increases accordingly.

These facts are so commonly observed and are so obvious and reasonable that they become incontrovertible. Yet the author's conclusions seem to be in reverse in every particular.

Synchronization of Time of Melting

The ratio of the CO_2 to CO is the governor of the cupola which synchronizes the time of melting a charge of iron to agree exactly with the time of burning a charge of coke. This regulation is neither perplexing nor baffling, for the known laws of nature take care of the situation automatically.

One lb. of coke may melt 7-8-9-10-11-12-13-14-15 or 16 lb. of iron. In each case, the 1 lb. of coke must be burned in exactly the same time required to melt the iron; also, the 1 lb. of coke must develop the required amount of heat at the required temperature to melt the iron in the exact time required to burn the coke.

If the iron charge melts in less time than required to burn a charge of coke, then a new charge of coke comes down ahead of the scheduled time and increases the height of the bed coke, and if this condition should continue indefinitely, the cupola will become filled with coke, with no room for an iron charge.

If the iron charge is delayed in melting beyond the time to burn a charge of coke, then the bed coke is burned away until a new charge of coke is released, and if this condition continues the space becomes filled with iron, at which point melting would cease.

How, then, is exact synchronization achieved? Simply by the laws of nature, over which we have no control when the coke volume and air volume are constant. The operation in detail is easily explained. For example, in the first case mentioned, the iron is melted too soon, the bed coke increases, the depth of the reducing zone increases, a greater proportion of CO_2 is reduced to CO, thereby lowering the temperature of the gases, which has a tendency to retard the rate of melting; also, that portion of the coke which is used to reduce CO_2 does not reach the combustion zone, and since there is less coke to burn the time required to burn a charge is reduced. The combined action is to increase the time of melting and reduce the time of combustion until equalization is reached.

In the second case, where the time of melting a charge is retarded and the depth of the reducing zone decreased, less CO_2 is reduced to CO. Consequently, the temperature is increased and also the heat developed is increased, resulting in a decrease in the time required to melt a charge of iron. Because less coke is used in the reduction of CO_2, there is more coke to be burned per charge and, consequently, the time of burning is increased, the combined action being to decrease the time for melting

an iron charge and to increase the time of burning a coke charge. This action continues until exact synchronization is established, after which there is a constant fluctuation of 1 per cent or more in the percentage of CO_2 in the effluent gases, which maintains the synchronism indefinitely. This regulation is just as positive as a governor on a corliss engine which, when once set, controls the speed of the engine without attention from the operator.

Test of Author's Formulae When Blast Is at Atmospheric Temperature

The author has not followed the conditions that exist within the cupola, hence his formulae are not readily adapted to the solution of problems that are peculiar to the cupola. In fact, the substitution of rate of burning for the amount of heat developed in the formulae is fundamentally wrong and leads to *reductio ad absurdum*.

The test of the value of an algebraic analysis of any set of variables is the "useability" of the expressions in solving for any unknown quantity within the group. On this basis, the value of the 75 formulae may be tested by their application to the solution of the following problem:

Problem. A new cupola is to be installed to melt at the rate of 16 tons per hr. for 5 consecutive hours, temperature of the iron to be 2800° F. and silicon loss less than 20 per cent of the silicon charged. Solve for:

1. Range of suitable iron-to-coke ratios.
2. Blast pressures or air volumes per min. to correspond with iron-coke ratios that are available.
3. Range of percentages of CO_2 for available iron-to-coke ratios.
4. Percentage of coke burned in combustion zone.
5. Percentage of coke used for reducing CO_2.
6. Total heat developed.
7. Heat loss by reduction of CO_2.
8. Heat available for melting iron.
9. Lbs. of iron melted for each lb. of coke charged.
10. Size of cupola required.
11. Combustion efficiency—Absorption efficiency—Over-all efficiency.

If the author's formulae in the hands of a competent foundry superintendent will solve this simple problem, then the author has made a valuable contribution to the foundry industry. If the test should fail, then it remains for a further analysis of the conditions within the cupola to be made and formulae developed in harmony with these conditions, and which can easily be applied by a person of ordinary intelligence to solve the general problems required in the previously stated example.

The author concludes his studies with formula

$$D = 21.5 - 0.51X, \tag{75}$$

in which D = rate of melting in tons per hr. and X the percentage of CO_2.

The graphic diagram illustrating the curve developed by the equation is shown as Fig. 1. These conclusions are very prominent, coming as they do at the closing pages of the paper, and the restrictions which the author places upon them are not clear.

In the absence of any other diagram, both the formula and the chart are misleading and are subject to fallacious conclusions, at least as far

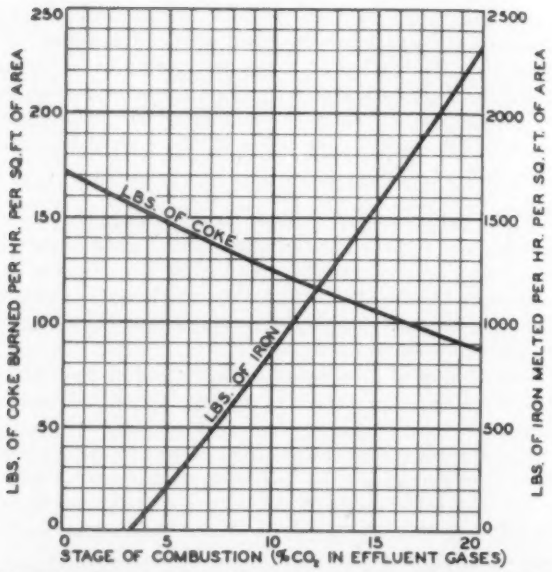

FIG. 2—RELATION OF BURNING RATE AND MELTING RATE TO STAGE OF COMBUSTION. AIR-VOLUME CONSTANT AT 200 CU. FT. PER MINUTE.

as the ordinary reader is concerned. The application of the formula indicates that when conditions are introduced which produce the same results as are obtained by lowering the blast pressure and lowering the iron-coke ratio, more rapid melting is the result, which, of course, is the reverse of experience. The following table expresses the ordinary interpretation:

	Rate of Melting	
	Max. Rate	*Min. Rate*
CO_2, per cent	0.0	21.0
B.t.u. developed per lb. of carbon	4350	14550
Gas formed, lb. per lb. of carbon	7.0	13.0
Temperature of gas, ° F.	2300	3500

If the CO_2 should be increased to approximately 42½ per cent by the introduction of oxygen, the temperature of the gases will be materially increased and, according to the formula, the rate of melting would be reduced to zero, which indicates a frozen cupola. However, if oxygen should be added until the CO_2 becomes 100 per cent the formula indicates the rate of melting to be minus 29½ tons per hr., whatever that may mean.

There is no discussion in the paper to show the relationship between various air volumes and percentage of CO_2, which is the basis for the determination of iron-coke ratios.

Using the author's formula (47) and substituting various values for X when C is constant at 200 cu. ft. per sq. ft. per min., the iron-coke ratios and the rate of melting are calculated and shown in Table 5 and plotted

in graphic diagram (Fig. 2). This shows that melting ceases when the percentage of CO_2 is below 5 per cent and the rate of melting is inversely to the rate of burning. This is a very different result from that shown in the author's Fig. 1.

The rate of burning and the rate of melting are shown on the graphic diagram (Fig. 2).

The algebraic development of the relationship between certain fundamentals within the cupola comes to an end with equations (70) and (75) for the rate of melting; in fact, the rate of melting seems to be the paramount issue in the entire discussion. No attempt is made to show

Table 5

APPROXIMATE HEAT BALANCE—FROM COMBUSTION ZONE THROUGH MELTING ZONE—FOR VARYING PERCENTAGES OF CO_2†

	Per Cent CO_2				
	Zero	*5*	*10*	*15*	*20*
Wt. of gases of combustion, lb.	7.03	8.05	9.32	10.97	13.16
B.t.u. developed by combustion	4350	6050	8175	10910	14550
B.t.u. from sensible heat in carbon	400	580	750	1000	1300
Total B.t.u. developed	4750	6630	8925	11910	15750
Sensible heat in gases leaving melting zone at 2500° F., B.t.u.	4725	5520	6820	8090	9480
B.t.u. available for melting, slag formation, superheating of iron and slag	25	1110	2105	3820	6270
B.t.u. required for slag formation, radiation, etc.	800	850	900	950	1000
B.t.u. remaining for melting and superheating	−775	260	1205	2870	5270
B.t.u. required for melting and superheating 1 lb. of iron	160	170	180	190	200
Iron melted per lb. of carbon, lb.	0	1.5	6.7	15.6	26.4
*Carbon burned per sq. ft. of area per hr., lb.	172	148	125	106	88
Iron melted per sq. ft. of area per hr., lb.	0	226	838	1633	2340
60-In. Cupola—20 Sq. Ft. Area					
Carbon burned per hr., lb.	3440	2960	2500	2120	1760
Iron melted per hr., tons	0.00	2.26	8.37	16.33	23.40
Carbon burned per ton of iron melted, lb.		1310	300	130	75
Gas formed per hr., tons (approx.)	11.7	11.7	11.7	11.7	11.7
Temperature of gases when formed, ° F.	2500	2900	3200	3500	3700
B.t.u. developed per hr. (thousands)	1634	1962	2231	2525	2772
74-In. Cupola—30 Sq. Ft. Area					
Carbon burned per hr., lb.	5160	4440	3750	3180	2640
Iron melted per hr., tons	0.00	3.39	12.56	24.50	35.10
Carbon burned per ton of iron melted, lb.		1310	300	130	75
Gas formed per hr., tons (approx.)	17.5	17.5	17.5	17.5	17.5
Temperature of gases when formed, ° F.	2500	2900	3200	3500	3700
B.t.u. developed per hr. (thousands)	2451	2944	3347	3787	4158

† Air volume constant at 200 cu. ft. per min. per sq. ft. of cupola area.
*Carbon burned per hour calculated from formula (47).

the application of the rate of melting to cupola problems, with the single exception of its adaptation, in a roundabout way, to the checking of air-volume-meters. The theoretical amount of air per min. used in any case is simply the cu. ft. of air required for one lb. of coke, multiplied by the number of lb. of coke which disappear per min. It is not clear why a complicated algebraic expression should be used.

At the conclusion of the algebraic treatment the author presents a number of conclusions which he considers axiomatic and hence require no analysis. Some of these assumptions seem to be fallacious, paradoxical and diametrically opposed to ordinary observation of cupola operation, and thus throw a cloud over the entire analysis.

First fallacy. It is assumed that the percentage of CO_2 in the effluent gases depends entirely upon the iron-carbon ratio and is not affected by variations in air volume.

The basis for this conclusion lies in the interpretation of certain reports of Jungbluth and Korschan[5], also Piwowarski and Meyer[7]. In one of the reports referred to, the air volume was varied from 210 to 530 cu. ft. per min. per sq. ft. of cupola area, representing a difference in blast pressure from 4 to 25 oz. The iron-coke ratio varied from 8.6 to 9.6 lb. The CO_2 analysis varied irregularly from 12.2 to 15.0 per cent CO_2.

The conclusion drawn from this record that the percentage of CO_2 is not affected by this wide variation in air-volume does not ring true when compared with universal observation. If the conclusion is true, then the results of an extended research by the Bureau of Mines is false, for they found a direct relationship between the time the CO_2 is in contact with incandescent coke of various temperatures and the per cent of CO developed.

If wide variations in air volumes have no effect on the per cent of CO_2, then these wide variations have no relation to oxidation, in which case air-volume and air-control meters are valueless, and CO_2 recording and regulating meters have only an imaginary value.

If the percentage of CO_2 is always constant for air volumes when the iron ratio is fairly constant, then the amount of heat developed per lb. of coke must be constant, and the rate of burning and the rate of melting are directly proportional to the air-volume, and these constants should be used directly in the equations, thereby eliminating all variables and unknown quantities. The fact that this is not done is an indication that the premise is not true.

It is certainly not necessary to go to Germany to ascertain the relation of increasing and decreasing air-volumes to the percentage of CO_2 developed when we have our daily charts which record the results in a positive and unmistakable manner. These charts show a decided sensitivity in the relationship between the percentage of CO_2 and small changes in air-volume. It is this sensitive relationship that makes the use of air control meters and CO_2 recorders invaluable in the almost instantaneous correction of swings in the cupola toward the hard or soft side, to which the author refers in paragraph 62 as at times materially affecting the percentage of rejects for the entire heat.

In this case it is not the increased rate of melting that does the harm but the increased flow of air which increases the CO_2 percentage, thereby increasing the oxidation that does the damage. If it were necessary to change the iron-carbon ratio to correct this condition, it would be necessary to wait an hour to melt out the charges already in the cupola before the corrected charge would appear in the reservoir ladle, whereas the correction is immediate by means of a change in the air-volume. It is simply a matter of the air-coke ratio, in which it is much simpler, and more immediately effective, to change the air than to change the coke.

It will require more evidence than the mere statement of an individual to convince cupola operators that air-volume is not an element in the control of CO_2, or that 7500 cu. ft. of air per min. passing through a cupola having 10 sq. ft. of area produces identical results when the same amount of air in the same time is passed through a cupola having an area of 30 sq. ft., or that air-volume control is not the most effective manner of maintaining uniform melting conditions.

Second fallacy: Referring to Fig. 1, p. 1011 of text. The second fallacy lies in the assumption that because the rate of coke disappearance increases as the percentage of CO increases, the rate of melting and the rate of coke disappearance follow the identical curve which is also the inverse curve of the percentage of CO_2 developed, as indicated in the chart.

Iron is not melted by CO or CO_2, but by the heat that is released in the development of these gases. Heat released when carbon is oxidized to CO_2 is three and one-third times greater than when oxidized to CO; hence if the rate of burning is doubled by increasing the percentage of CO from zero to 34 per cent, the heat developed will be reduced to 60 per cent of the original; hence the heat available for melting grows less very rapidly as CO increases, for two reasons—first, the total heat is less; second, the temperature is reduced more and more nearly to the temperature required for melting iron.

The amount of heat is not the only factor that determines the iron-to-coke ratio. It is the amount of heat in the gases above 2500° F. that determines how much iron can be melted.

When combustion gases are reduced completely to CO the temperature of the resultant gases is below the temperature required to melt iron, and hence melting ceases, which is the exact reverse of the condition shown in the author's Fig. 1.

If the author's conclusion, as expressed in Fig. 1, is correct, then iron should be melted in a producer gas furnace, for, in addition to the production of the desired gas, the maximum amount of iron can be melted without additional cost.

However, our experience shows that when a cupola is converted into a gas producing furnace, which can be done in less than five min. by air manipulation, melting ceases. This is positive proof that the two operations cannot be conducted in the same furnace simultaneously.

Other fallacies are contained in the 14 conclusions summarized in paragraph 79. These will be discussed in order.

Conclusion 1: "In the burning of carbon, CO is formed stepwise through the intermediate product, CO_2."

This conception is a distinct aid in the analysis of heat formation and heat distribution in the cupola. Only a part of the coke in the charges reaches the combustion zone. Notwithstanding this conclusion, it is unfortunate that the author does not use it in the development of his equations; consequently, there is no relationship between the two steps that have been so clearly outlined, and the derived equations.

The portion of coke which reaches the combustion zone is burned. The portion that is used directly in the reduction of CO_2 to CO is not burned. Burning is an oxidizing process. It is improper to apply the term burning to a reducing process.

Conclusion 2: "The correct mechanism of fuel reactions is not needed to make a heat balance, etc."

The author dismisses the entire question of heat formation, temperatures, critical points that determine iron-carbon ratios, efficiencies, cupola control, etc., by the remark that the correct mechanism of fuel reactions is not necessary to make a heat balance. Apparently the objectivities of efficiencies and cupola control are considered to be so unimportant as to require no analysis.

As a matter of fact, it is the study of these items that is the only justification for applying mathematics to cupola problems. Heat balances are just as essential to the understanding of cupola economics as financial balances are to the analyses of business enterprises.

Conclusion 3: "A hot cupola tends to form more CO."

In the absence of a more definite explanation the statement is misleading, for a hot cupola indicates higher velocities in the gases which oppose the formation of CO.

Higher velocities = less time for reduction of CO_2 to CO.
Less time = less CO.
Less CO = more CO_2.
More CO_2 = more B.t.u. per lb. of coke.
More B.t.u. = higher temperature.
Higher temperature = faster melting.
Faster melting = hotter iron.

These results do not correspond with conclusion 3.

Certainly, the reverse of the proposition is not true, namely, that the higher the percentage of CO the hotter the furnace. The temperature of the cupola is regulated by the iron ratio to a greater extent than by the reduction of CO_2 to CO. The cupola operator increases the iron ratio until the temperature is reduced to the desired point.

In the Belden[1] researches, from which the author draws many conclusions, the cupola was filled to the charging sill with coke. No iron was melted; hence there is no relation between the temperature of this furnace and the rate at which CO is formed as compared with a cupola melting iron in which each lb. of coke in the combustion zone (the amount of

coke required for reducing CO_2 to CO excluded) has from 12 to 20 lb. of liquid iron flowing around it, resulting in a very rapid transfer of heat to superheat the iron. The hottest part of the cupola is at the combustion zone where the percentage of CO is at a minimum.

In a graphic diagram representing the results of experiments at the Bureau of Mines, it is shown that at a temperature of 2000° F. 20 per cent of CO may be developed, while at 2600° F. only 10 per cent of CO is developed, the difference being accounted for by the time the CO_2 is in contact with the incandescent coke.

There are three items to be considered in the development of CO — the temperature of the reducing zone; height of the column of incandescent coke; velocity of the gases. Two of these items fix the time of contact; and time and temperature determine the rate at which CO is produced.

The author is in error in considering only one of the controlling elements.

Conclusion 7: "Melting rate in tons per hr., iron-carbon ratio, air-volume, percentage of CO_2 in the cupola effluent, and the ratio of carbon dissolved to that burned" are grouped as a family of variables called "A." All other variables are placed in a family called "Z."

The advantage gained by this grouping is not obvious.

Conclusion 8: Under this conclusion, the entire number of equations (5) to (70) may be considered. These 66 equations are reduced to three final forms:

(1) Ratio CO to CO_2, per cent.
(2) Carbon burned per hr., lb.
(3) Iron melted per hr., tons.

Then

$$(9)\quad Y = 1.656\ (20.8 - X)$$

$$(47)\quad B = 1.542C\left(\frac{52.48 - X}{94.13 + X}\right) = 0.42C \text{ when } X = 20.8$$

$$(70)\quad D = 7.71 \times 10^{-4}\ RC\left(\frac{52.48 - X}{94.13 + X}\right) = \frac{0.42CR}{2000}$$

These equations can be written directly without intervening equations. We know before we start that when per cent CO_2 = zero per cent CO = 34.44, the ratio is

$$(1)\quad \frac{34.44}{20.8} = 1.656$$

$$(2)\quad B = \frac{60}{142.3} = 0.42\ C \text{ lb.}$$

$$(3)\quad D = \left(\frac{0.42\,C}{2000}\right)\frac{R}{\text{per cent carbon burned}}$$

$C =$ 1 cu. ft. per min. = 60 cu. ft. per hr.
142.3 = cu. ft. of air to burn 1 lb. carbon
Carbon disappeared equals carbon burned plus carbon used in reducing CO_2
$R =$ iron-carbon ratio (carbon disappeared).

There is no advantage in loading up these simple equations which solve the problem with cumbersome algebraic equations which in the end do not solve the problem.

In paragraph 70, the author stresses the exactness of his equations as compared with the simpler ordinary arithmetical method. This claim is baseless since both methods are derived from the initial chemical formula, the only difference being that in this discussion the arithmetical calculation is carried a step further to produce two simultaneous equations whereby the variables can be calculated, while the algebraic development has but one equation for the variables and hence is indeterminate.

Conclusions 13 and 14, and paragraph 18: If these conclusions and the statement in paragraph 18 are true, it would seem that the cupola operator is quite helpless in controlling results, for, if the coke happens to be of larger or smaller pieces (a condition which he cannot control), the efficiency of combustion is changed. An item of far greater importance, with reference to coke, is its reactivity, of which size is only one of the influencing factors. Also, a colder blast is said to produce higher combustion efficiency and yields colder iron. The cupola operator has no control over blast temperatures in ordinary cupolas.

Also, these conclusions are based upon the theory that each iron-carbon ratio has its own definite combustion efficiency indicated by a definite percentage of CO_2 in the effluent gases, over which the operator has no control.

It would then seem quite impossible for the cupola operator to select the optimum iron-carbon ratio to correspond with variable coke sizes, variable temperatures of the blast, resulting in variable percentages of CO_2, which in turn produce variable combustion efficiencies.

Also, according to these conclusions, at times more CO is accompanied with colder iron, and at other times more CO is accompanied with hotter iron. It is clear that it is not the percentage of CO that determines the temperature of the iron. As a matter of fact, the cupola operator is in direct control of combustion efficiency by his control of blast pressure.

At every cupola the operator chooses the optimum CO_2 percentage (combustion efficiency) which, in a large measure, fixes the degree of silicon and manganese loss, and the amount and temperature of iron that can be melted with one lb. of coke. If the coke is small, the blast pressure is increased to a point where the desired CO_2 is reached. The same is true for iron temperature. Regardless of the temperature of the blast, the operator can control combustion efficiency at 10, 12, 14 or 16 per cent CO_2 by simply regulating the blast pressure. The higher the pressure, the more CO_2; the lower the pressure, the less CO_2.

In case of changes in the iron-ratio, the smaller amount of iron passing over the bed coke will naturally be superheated to a higher degree than when a large quantity of iron passes through the same zone.

The control of all these variables is reduced to the amount of time required for the gases to pass through the various zones. The time is fixed by velocity, and velocity is fixed by blast pressure. Hence the final regulating item to harmonize all conditions with the desired condition is the blast pressure; and the temperature of the iron is fixed by the iron ratio. Everyone wishes to melt as much iron as possible per lb. of coke, avoiding oxidation on the one hand and obtaining the desired temperature of iron on the other.

Under conclusion 3, the effect of increasing blast pressure was shown. Similarly, the effect of lowering the blast may be shown here:

Less air pressure = lower velocity;
Lower velocity = more time in reduction zone;
More time = more CO_2 reduced to CO;
More CO = less CO_2;
Less CO_2 = lower temperature;
Lower temperature = slower melting;
Slower melting = colder iron.

These conclusions are directly opposed to the conclusion in paragraph 72 that CO_2 is not affected by reasonable changes in air volume. It is this far-reaching conclusion that effectually blocks consideration of cupola control and stands in the way of the recovery of waste heat that is responsible for this discussion. Both theorems cannot be correct.

It is regrettable that it should seem advisable to present so vigorous a criticism of a paper on cupola fundamentals. Apparently the author has failed to simplify any of the formulae that are in use by combustion engineers for analyzing the fundamentals of combustion. The field covered, in comparison with the large number of fundamentals involved in cupola practice, is exceedingly small, and the results are not in a form for use by the ordinary cupola operator. Great stress is laid upon minor items, such as changing the composition of the air; changing the weight of metal by reason of the absorption of carbon; the correction of CO_2 analyses to account for the CO_2 in the limestone of the flux; the method of checking volume-meters by the formula for the rate of melting.

The two equations which are of most importance are for

1. Rate of burning;
2. Rate of melting.

These are of limited value, because it is necessary to go to the cupola record to obtain the known quantities to be substituted into the formulae, which results in little gain because the results are already recorded in the daily cupola record.

There is thus little else left of the laborious effort in the derivations of algebraic formulae.

All these items, however, are unimportant and would not warrant a discussion were it not for the conclusions introduced without mathe-

matical support that virtually block consideration of cupola control and make the cupola operator an automaton.

The conclusions to which exception is taken are:

That blast volumes have no effect on combustion efficiency.
That each size of coke has its individual combustion efficiency.
That each iron-carbon ratio has its own combustion efficiency.
That preheating the blast has no effect upon the iron-carbon ratio.

It is statements like the above which I wish to emphatically challenge, and to give testimony to the high responsibility placed upon the cupola operator and to the skill which he exercises to produce the desired results regardless of the variables with which he is constantly confronted, such as coke size; iron-carbon ratios; variation in blast temperature; varying compactness of the charge resulting in greater or lesser resistance to the passage of the gases; the irregular spreading of the coke charge resulting in irregularities in the depth of the bed coke and causing irregularities in combustion; irregularities in blocking the tuyeres, etc., all of which call for ceaseless vigilance to counteract and produce a uniform result. He cannot call upon the general office for detailed instruction, but must act on his own initiative.

It is hoped that this discussion may stimulate the study of the reactions which take place within the various zones of the cupola, and that the industry at large may separate the wheat from the chaff.

DR. PENNINGTON *(answer to Mr. Vial's written discussion):* In spite of the many variances in opinion that Mr. Vial and I have had pertaining to desirable cupola operation, I wish to express indebtedness for the study which resulted in his long discussion. The fact that he has helped to focus attention upon certain very definite controversial issues should lead to an earlier solution of some of them. To the reader, it may at first appear that there are two schools of thought, the critic being the leader of the "heat balance school" and the author a proponent of the "material balance school." However, this is not at all the case.

It is true that I stress the importance of material balance and that he makes many applications of the principles involving heat balances. Both of us realize the importance of adequate heat balances and, moreover, are aware that a heat balance cannot be made unless there is first a material balance, either measured or assumed, upon which the calculations can be based. There can be no conflict between the two; they are absolutely interdependent. A good material balance will lead to a good heat balance; a poor one may lead to the impossible.

Here is where the controversy really gets its start. It is my contention that the material balance must first be established before there is justification for the calculation of a heat balance. I maintain that one process of cupola operation cannot be proved to be superior to another by assuming material balances and calculating therefrom heat balances to prove the point. It would be just as fitting to assume the heat balance and save the calculations because the heat balance that is obtained is absolutely fixed by the assumed material balance whether it is accurate or

not. Since the heat balance is so definitely dependent upon the material balance, it makes a considerable difference whether the cupola effluent contains 12 or 14 per cent CO_2. In comparing two operations, why assume 12 per cent for one and 14 for the other? Why not take 14 for the former and 12 for the latter? For that matter, why not assume 13 per cent for each one? Heat balances representing these conditions will differ quite significantly. In view of this difference, I wish to state with emphasis that I consider it imperative, in comparing one operation of a cupola with another through their heat balances, that adequate measurements must be made to establish a reasonably accurate material balance.

In regard to a comparison of the hot blast with the cold blast, the critic and the author are not on opposite sides. Both of us think that the hot blast will yield hotter iron. How much hotter? I do not know nor am I able to guess from the heat balances offered by the critic because he has not given authority for the material balances which he has used. He would say that the hot cupola gives hotter iron because of the increased sensible heat in the blast and because more CO_2 is produced. I would say that hotter iron is produced because of the higher sensible heat but in spite of there being less CO_2.

Having discussed the difference in viewpoint on the broad general principles, it is in order to reply concerning some of the details. In the first paragraph of Mr. Vial's discussion the implication is made that combustion in the cupola is entirely different from what it is in other furnaces. The properties of hot coke and air are such that they react where they come in contact with each other; it really does not matter that the contact is made within the cupola. No evidence has been given which would warrant the conclusion that cupola combustion is in some mysterious way substantially different from any other combustion. The mathematical relations presented in the paper do apply to the cupola if properly used. This point can be settled more properly by making applications pertaining to actual operations rather than through a copious use of words.

I regret that the purpose of making the mathematical developments and derivations was not more clear. An attempt was made to show how some of the equations could be put to practical use. Some of the equations were merely used in the derivation of others and were included so as to enable the reader to follow the syllogistic developments with considerably more ease. Occasionally an equation was given to show what the modification would be under slightly different conditions. Table 3 was given to assist the reader in his survey of the uses of the most important equations.

In this connection, reply will be restricted to equations (16) and (70). Equation (16) has been applied quite satisfactorily in a situation where a 72-in. (inside diameter) cupola was used in plant operation, the fuel being by-product coke. It has been evident from the results which have been obtained that the CO content could be calculated from the CO_2 as accurately as it could be determined by absorption, first in acidic cuprous chloride and then in cuprous sulfate-beta naphthol, with an appreciable saving of time.

In a recent investigation, it was suspected that CO was reacting with the corrosion products of the copper inner-cylinder of the gas sampling tube. If such oxidation was taking place, it was due to a reaction with an oxide of copper. Since CO does react with copper oxide (CuO) at some 600°F., it was decided that samples of the effluent gas would be passed over hot CuO and subsequently analyzed. Other samples taken directly from the cupola were also analyzed so that the change due to the oxidation could be ascertained. It was found that most of the CO was oxidized to CO_2 by the CuO according to the equation,

$$CO + CuO = CO_2 + Cu. \tag{76}$$

CO and CO_2 are related in equal volumes, one for one, whereas in equation (4) there are two volumes of CO for one of CO_2. But equation (4) was used in the development of (16). Therefore, if some such reaction as (76) takes place, equation (16) cannot hold.

A sample of data is given in Table 6 to show, first, that equation (16) does apply in the case of the regular cupola effluent and, second, that the equation becomes absurd if some of the CO is oxidized by CuO. If equation (16) does not hold where such an oxidation does take place, but does hold for the regular effluent collected through a water cooled copper tube, it follows that the corrosion products could not have oxidized an appreciable amount of the carbon monoxide. This use of equation (16) to prove that a specific type of reaction does not take place is only one of its many important uses. It may also be used to determine whether the gas sampling train is leaking. If through its application the CO is found considerably lower than the observed value, there is probably a leak. If, on the other hand, the calculated and observed values agree there can be no leakage of significant magnitude.

Equation (70) has many uses, two of which are especially worthy of mention at this point. It may be used to determine the volume of air which goes through the cupola if the iron-carbon ratio, melting rate, and percentage of CO_2 are known. Most wind instruments measure the air which passes some point in a pipe. Obviously, due to leaks, all of it does not go through the cupola.

This equation of A-variables may also be used in the interpretation of published data. Being a self-contained system in four variables, it is only necessary to know the value of three that the fourth may be

Table 6

ANALYSES OF CUPOLA GASES AND OXIDIZED PRODUCTS

Sample No.	*Gas*	*CO_2 Per Cent*	*O_2 Per Cent*	*CO, Per Cent*	
				Observed	*Calculated*
1	Regular	11.3	0.2	16.7	16.4
1	Oxidized	28.3	0.0	0.4	−11.8
2	Regular	7.8	0.1	22.4	22.5
2	Oxidized	29.9	0.1	0.9	−14.7
3	Regular	8.3	0.2	21.8	21.5
3	Oxidized	29.5	0.2	0.4	−14.2

evaluated. It follows that, if the iron-carbon ratio is constant with a constant air-volume, faster melting must be accompanied by a decrease in CO_2 in the cupola effluent gas regardless of what change may have been made to effect the faster melting. Constant air-volume will remove more coke and consequently more iron from the cupola in the case where the CO is higher.

If an investigator reports data which do not include the air-volume, the equation may be used to calculate it. If the CO_2 content of the effluent gas is not given, it will be necessary to calculate it first by use of equations (9), (77), and (78). The value of X thus obtained may be substituted along with the values for iron-carbon ratio and melting rate into the equation of A-variables to find the air-volume per min. An apparent superiority of one process over another may actually be due to the use of more air.

In raising the question as to one's right to change the composition of the air, the critic has convinced me that I failed to make it clear why 20.8 was used instead of 20.93 for percentage of oxygen in air. I accept the average of 20.93 reported by Clark[2] as the most probable oxygen content of the air and had no intention of altering this value for the purpose of obtaining a more accurate one as far as the composition of the air itself is concerned. In making the mathematical derivations, it was assumed that the cupola effluent gas contained carbon monoxide, carbon dioxide, and nitrogen only. All these gases in some way come from the air. In addition to them there is at least one other gas, hydrogen, which comes from the coke. This hydrogen must be taken into consideration either directly or indirectly.

If it is to be considered directly a symbol representing percentage of hydrogen will occur in the formulas. In an effort to reduce the number of variables as low as possible, it was decided to consider it indirectly by obtaining an "effective oxygen content of the air" by blank cupola runs. This is the reason why 20.8 was chosen in preference to 20.93. After establishing the value for Z from the data of Belden[1], it is not necessary to make a further correction for hydrogen or, for that matter, it is not essential to know that it is present. However, it should be borne in mind that a different kind of fuel will require a new evaluation of Z.

As a matter of illustration, a composition of cupola gas which is in harmony with the equations has been selected to show just how the hydrogen affects the value of Z. The composition is shown in the following tabulation:

Substance	*Per cent (volume)*
Total CO_2	10.73
CO_2 (coke)	9.90
CO_2 (limestone)	0.83
CO	17.75

The hydrogen, which comes from coke, can be determined by analysis of the cupola effluent or it can be calculated from the weight percentage of hydrogen known to be in the coke. A typical value for the hydrogen content of the by-product coke currently used in our plant experiments

is 0.30 per cent by weight. On the basis of fixed carbon alone the weight percentage is 0.34. Inasmuch as each molecule of carbon gas contains one atom of carbon, and one atom of carbon is six times as heavy as a molecule of hydrogen, the volume of hydrogen from the coke will be 2.04 per cent (obtained by multiplying 0.34 by 6) of the total volume of CO and CO_2 both from coke. The total CO and CO_2 in the situation being considered is 27.65 per cent. The percentage of hydrogen which comes from the coke in the cupola effluent may be given as the product of 0.0204 and 27.65 which is 0.56 per cent.

Owing to the presence of this hydrogen, the CO and CO_2 are lower than they would be if the hydrogen were not there. If one is interested in determining the "actual oxygen content" in contrast to the "effective oxygen content" the CO and CO_2 should be corrected upward allowing for the hydrogen. Substitutions can then be made in equation (10). An alternate choice would be to correct for both hydrogen and CO_2 from limestone and to make the substitutions in equation (6). The latter alternative has been chosen. The total of the hydrogen from coke and the CO_2 from limestone in the problem under consideration is 1.39 per cent. The CO and CO_2 may be corrected to a basis free of hydrogen and CO_2 from limestone by dividing by 0.9861. Such corrections would change the percentages of CO_2 and CO from 9.90 and 17.75 to 10.04 and 18.00. Substitution of the last two values in equation (6) will give 20.92 as the percentage (Z) of oxygen in air. If 20.75, instead of the rounded off value of 20.8, had been used in selecting the percentage of CO corresponding to 10.73 per cent CO_2, 20.88 would have been obtained for Z. If the hydrogen content were a little more, it is conceivable that Z could well be 20.93. Again I wish to say that I accept 20.93 as the most probable value for the oxygen content of air, but, if the hydrogen from coke is to be ignored in the cupola effluent, a value of 20.8 is to be preferred in the mathematical equations. It is a matter of introducing an empirical correction for an error.

As to the effect of air-volume upon the analysis of the cupola gas, I am inclined to join the critic in opposing Jungbluth and Korschan[5], and Piwowarsky and Meyer[7] in their contention that the CO_2 is constant with varying air blast. The formula,

$$\text{Per cent } CO_2 = R + 0.01C,$$

is interesting but no evidence was presented to show how it was derived. We have been able to get, in the 72-in. cupola, as low as 7.5 for percentage of CO_2 where R was 7.0. In order for the above equation to hold, C would be 50 cu. ft. per sq. ft. of cupola area, or a little more than 1400 cu. ft. per min. in a 72-in. cupola. The air-volume at the time that the CO_2 was 7.5 was certainly not very close to 1400 cu. ft.

Data have been taken from the fourth grouping in Table 1, p. 87 of the article by Jungbluth and Korschan[5], which has been cited, to test the equation given above. These results are shown in Table 7. The first column contains blast volume in cubic meters per min. Column 2 shows the volume in cu. ft. per sq. ft. of cupola cross section. The third column was obtained by dividing kilograms of carbon per 100 kilograms of iron

Table 7

RELATION OF AIR-VOLUME TO PERCENTAGE OF CO_2

Air-Volume			*Percentage of CO_2*	
Cu. Meters	*Cu. ft./ Sq. ft.*	*R*	*Observed (Jungbluth and Korschan)*	*Calculated (Vial)*
15.0	210.2	9.62	13.4	11.7
19.7	276.1	9.52	14.2	12.3
20.0	280.3	8.62	12.9	11.4
20.0	280.3	9.35	15.0	12.2
20.3	284.5	9.09	12.2	11.9
25.3	354.6	9.17	12.2	12.7
25.7	360.2	9.17	13.5	12.8
30.4	426.0	8.62	13.1	12.9
35.4	496.0	9.01	12.4	14.0
36.9	517.1	8.85	13.2	14.0
37.3	522.7	8.77	13.5	14.0
37.9	531.1	9.01	13.8	14.3

into 100 to get the iron-carbon ratio *(R)*. The fourth column represents *X*, the net percentage of CO_2 as measured. The fifth column gives the percentage of CO_2 as calculated by the formula,

$$\text{Per cent } CO_2 = R + 0.01C.$$

It is apparent from an examination of column 4 that there is no significant change in CO_2 with increase in air-volume. A curve has been fitted to the data by law of least squares and is shown in Fig. 3 as the equation,

$$\text{Per cent } CO_2 = 13.63 - 0.0009C.$$

The curve which represents the critic's equation is also included in Fig. 3. In the equation representing the experimental data, the coefficient of *C* is so small that the entire term may be neglected, in which case the percentage of CO_2 is constant with changing air-volume throughout the range which has been considered.

In the operation of the 72-in. cupola, we have not been able to vary the air-volume through nearly so wide a range as Jungbluth and Korschan[5] did in their 21.5-in. cupola. Therefore, our results do not appear as convincing as those experimental measurements shown in Fig. 3. The practical limitations imposed by casting schedules have restricted the air-volume to a range of 6800 to 7600 cu. ft. per min. Per sq. ft., the range would be 240 to 270 cu. ft. per min. The results obtained with such limited variations have not yet given the answer to the effect of air-volume on the percentage of CO_2. However, the indications are that, in the range in which we have worked, an increase in air-volume will yield higher CO_2 as claimed by the critic. It should be pointed out that there are two essential differences in my technique and that of Jungbluth and Korschan[5]. They used a 21.5-in. cupola; I used a 72-in.

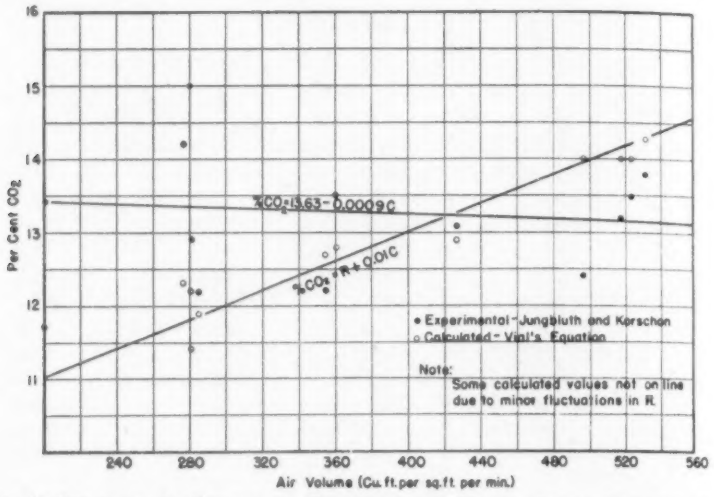

FIG. 3—EFFECT OF AIR-VOLUME ON PERCENTAGE OF CO_2 IN CUPOLA EFFLUENT GAS.

cupola. They used an air-volume range from 210.2 to 531.1 cu. ft. per sq. ft. per min.; I used a range from 240 to 270 which, besides being on the low side by comparison, is so narrow that small errors in measurements might hide the true relation.

There are two factors which are influenced by increasing the blast velocity. One, as the critic pointed out, is that less time is available for the chain of reactions to take place. Inasmuch as CO is the end product in the chain, shorter time will tend toward lower CO with higher CO_2. From this point of view, a faster blast will yield more CO_2. On the other hand, increased blast will increase the temperature, which will tend to speed up all of the reactions in the chain, resulting in the formation of more CO and less CO_2. The two factors are in opposition, and it is difficult to guess which will prevail. The coincidence that the effect of one factor will exactly compensate the other is hardly to be expected.

In spite of the fact that some doubt has been raised concerning the conclusion of Jungbluth and Korschan[5] as to the constancy of CO_2 with varying air-volume, their results are still accepted reservedly as the best that are available at the present time. Neither the critic nor I have offered data to substantiate our claim that the CO_2 of the cupola effluent gas is increased by increasing the air-volume. It was in this accord that some points were discussed in the paper regarding the CO_2 as being constant with changing air-volume. If it can later be shown that the CO_2 content does increase with increased blast, then it will no longer appear that a doubling of the air-volume will double the melting rate. It will fall short of being doubled according to the change in CO_2.

Because of incorrect interpretations of heat balances the critic has reached an erroneous conclusion embodied in the following statement: "It is almost axiomatic that the greater the percentage of CO_2 in the

effluent gases, the hotter the furnace and consequently the hotter the iron as melted." The chemist and metallurgist may find the statement in error through chemical analyses of gases coming from cupolas known to be melting iron at decidedly different temperatures. The practical foundryman may be convinced by his observations at the charging door. Calculation of the number of heat units per lb. of iron rather than per lb. of carbon will convince the thermochemist.

In appealing to the thermochemist, we have chosen to calculate the heat produced for different iron-carbon ratios for each lb. of iron melted. In making these calculations, it has been our preference to take data which have been measured for some cupola actually in operation. It is also preferable that these data be in the technical literature. In order to get the percentage of CO and CO_2 corresponding to different iron-carbon ratios we have used the equation,

$$N_s = 125.2 - \frac{958}{R} + \frac{2600}{R^2}, \tag{77}$$

taken from Fig. 3 of the article by Massari and Lindsay[3]. N_s is defined as

$$N_s = \frac{100\ (per\ cent\ CO_2)}{per\ cent\ CO_2 + per\ cent\ CO}.$$

In conjunction with (77), it is necessary to use (9) and the equation given immediately above in the form,

$$N_s = \frac{100X}{X + Y}. \tag{78}$$

Equations (9), (77), and (78) have been solved, as a system of simultaneous equations, for X and Y. If it is preferred, the values of N_s may be taken directly from the graph as given by Massari and Lindsay[3]. The results shown in Table 8 were obtained by use of equation (77) rather than the graph. The heats of formation of CO and CO_2 have been taken as 4350 and 14550 B.t.u. per lb. of carbon, respectively.

Column 6 shows that less heat is developed per lb. of carbon as the CO_2 content (column 3) decreases. This observation is in keeping with similar statements made by the critic. Column 7, which was obtained by dividing column 6 by column 1, gives the B.t.u. per lb. of iron. It is immediately obvious that there is an increase in the number of heat units per lb. of iron as the CO_2 decreases because of a decrease in iron-carbon ratio. What it amounts to is that with lower CO_2 there is less iron to carry away the heat. At the same time that there is less iron, there is more gas to dissipate the heat. The two factors tend to neutralize each other, but it is a matter of record in many plants that the cooling effect of the iron prevails and that hotter iron is produced with more coke, accompanied by a lower CO_2 content in the cupola effluent gas.

Before making the appeal to the practical foundryman certain facts regarding the nature of carbon-gases should be reviewed. It is generally recognized that carbon monoxide and carbon dioxide are the only oxides of carbon that can be produced in the burning of coke. A mixture of gas

Table 8

RELATION OF HEAT DEVELOPED TO CO_2 CONTENT

			Fraction of Carbon Converted to			
R	*Per cent CO*	*Per cent CO_2*	*CO*	*CO_2*	*B.t.u./lb. of Carbon*	*B.t.u./lb. of Iron*
14.0	7.07	16.53	0.300	0.700	11,490	820.7
13.0	7.92	16.02	0.331	0.669	11,174	859.5
12.0	8.90	15.43	0.366	0.634	10,817	901.4
11.0	10.01	14.76	0.404	0.596	10,429	948.1
10.0	11.27	14.00	0.446	0.554	10,001	1000.1
9.0	12.69	13.14	0.491	0.509	9542	1060.2
8.0	14.27	12.19	0.539	0.461	9052	1131.5
7.0	15.88	11.21	0.586	0.414	8573	1224.7
6.0	17.19	10.42	0.623	0.377	8195	1365.8

high in CO will burn; one high in CO_2 will not burn. CO is inflammable; CO_2 is a fire extinguisher. With these basic facts in mind let us observe a cupola, at the iron spout and at the charging door, operating with an iron-carbon ratio of 16.0 and melting iron at some 2500° F. The iron will appear sluggish and dull and no flame will be observed at the charging door. Let these observations be followed by an examination of a second cupola operating with an iron-carbon ratio of 7.0 and melting iron at some 2800° F. The iron will appear bright and clean and there will be an intense flame at the charging door especially if sufficient air-volume is being used. In these two cases the practical foundryman knows which iron is hotter by its appearance to the naked eye. Moreover, he knows which cupola yields the more CO by knowing that CO burns and by knowing which cupola has the flame at the charging door.

An appeal to the chemist or metallurgist will be confined to a request that he take an apparatus suitable for gas analysis to the scene of activity, make the desired measurements, and report his findings. Data are respectfully solicited from anyone who has found experimentally that a cupola melting iron at 2500° F. has an effluent gas lower in CO_2 than one melting iron at 2800° F., the materials going into the cupola being approximately the same but, of course, in different amounts.

With all the discussion pertaining to the composition of air, it is surprising that the critic was apparently not aware of limitation 6 as given in the paper. The limitation specifically states that the CO_2 cannot appreciably exceed 20.8 per cent if the equations are to be applied. In the face of the warning of this limitation, he has substituted 42.5 per cent, which is more than twice the limit of 20.8, in equation (75) to find a melting rate of zero tons per hr.! He went a step further and substituted 100 for percentage of CO_2, on the basis of using air containing 100 per cent oxygen, to get a melting rate of minus 29.5 tons per hr.

A similar mistake was made by using Table 5 to construct Fig. 2, supposed to discredit my Fig. 1. If selected points on the "iron-line" of Fig. 2 are divided by corresponding points (same CO_2) on the "coke-line," it will be immediately obvious that the iron-coke ratio varies from

about 1.4 at 5 per cent CO_2 to 26.7 at 20 per cent CO_2. The iron-carbon ratio varies in the same way that the iron-coke ratio does, and in no way can it be regarded as being constant. Not only must conditions include constant iron-carbon ratio for applicability of Fig. 1, but, as the caption underneath the figure specifically states, this constant ratio must be precisely 7.0.

If the coke contains 90 per cent fixed carbon, an iron-carbon ratio of 7.0 will correspond to an iron-coke ratio of 6.3. By trial it may be found that this iron-coke ratio of 6.3 appears in Fig. 2 at about 9.5 per cent CO_2. If he restricts the conditions to an iron-coke ratio of 6.3 ($R = 7.0$), there will be no other value to represent the percentage of CO_2, and, consequently, the two lines will be reduced to two points. One of these points, that representing the iron, should fall on the line in my Fig. 1. The two figures just discussed are not subject to the same limitations; therefore, it will be difficult to use one of them to invalidate the other.

G. P. PHILLIPS* *(written discussion)*: First of all I wish to state that I consider Dr. Pennington's paper of value because of the fact that he has focused attention on combustion problems in the cupola. These problems are difficult to solve because of the fact that we cannot get samples of gases at different levels from a cupola that is melting metal, and have a difficult job in getting a sample representative of the gases across the cross section near or below the charging door.

Belden's[1] valuable research on combustion of coke alone in a small cupola gave data on gas analyses, temperatures attained and shape of the melting zone when burning coke alone at a low blast pressure of 5.3 oz. This work is cited by Dr. Pennington and is not familiar to many foundrymen, as the bulletin has been out of print for many years. I recall the trouble I had some years ago in getting a copy to study from the Crerar Library in Chicago. This work did not show the catalytic effects of iron on gas reactions, particularly the reaction $2CO \rightarrow CO_2 + C$, which some investigators have shown to be of importance in cupola operation.

When we consider data on combustion and effluent gas composition by various investigators, starting with work cited by Moldenke and coming down through more recent years and including both American and European work, a wide variation in results attained and conclusions drawn are found. Some work indicates that optimum amounts of CO_2 and CO are necessary for best results, considering both temperatures attained and oxidation losses. On the other hand, numerous operators of balanced blast cupolas (mostly British) claim that they operate with CO content in effluent as low as 2 to 4 per cent, with very low oxidation losses and relatively low coke-iron ratios. British operators of "Poumay" cupolas claim CO content of effluent gases as low as 0.5 per cent. Both of these latter types use upper rows of auxiliary tuyeres and claim more complete combustion by supplying oxygen to upper coke bed layers to burn CO to CO_2. If data from all sources are correct for their operating conditions, then here is a fertile field for cupola research.

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The comments of Dr. Pennington on the hot blast cupola do not seem correct to me, nor do some of his conclusions on gas composition with hotter, faster melting.

In support of Mr. Vial's contentions, I can cite our own experience with hot blast cupolas. When starting up our first installation at Rock Island we left the coke-iron ratio the same as we had been using with cold blast. This was 1 to 7.5 — 1 to 8 with same blast weight per min. as we were using on cold blast. With this coke-iron ratio between charges we obtained iron, with blast temperature at about 700° F., at 2950° F. and at too fast a melting rate. Daily thereafter we reduced coke charges, and blast weight accordingly, to a final ratio of 1 to 10.5 to get iron at 2800 to 2850° F., and at the rate needed at that time. It seems obvious that these results would be impossible with decreasing formation of CO_2 and increasing formation of CO. It is also obvious that there is sufficient CO present in the gases to supply the heat necessary, when burned with air, to heat the blast to 600 to 700° F. The over-all efficiency of the melting operation was increased markedly. Savings due to reduction in use of fuel, refractories and flux led directly to a second installation at our Indianapolis Works. Dr. Pennington based his conclusions on the assumption that the CO_2 formed more quickly with hot blast and, therefore, has a greater distance and time to be reduced to CO in passing through the bed. I believe the first assumption to be correct, but we use considerably less coke per charge and have a narrower melting zone with hot blast operation, which seems to invalidate his second assumption.

I have considerable respect for the time spent and work done by Dr. Pennington on cupola combustion, and for the experience and knowledge of Mr. Vial, who has spent many years in studying cupola combustion and in the successful development of the hot blast cupola.

The whole picture, as shown by published data of these and other investigators, brings out the need for research to answer fully the many unknowns about which assumptions are made today, and to correlate the divergent results that are presented in this literature.

DR. PENNINGTON *(answer to Mr. Phillips' written discussion)*: The author is especially indebted to Mr. Phillips for his interest in this paper and his analysis of some of the points discussed.

As he pointed out, it is difficult to get gas samples at all different levels from a cupola melting iron. However, it is comparatively easy to get the samples if the melting zone and the zone at the extreme top are excluded. As far as the equations are concerned, X is the average CO_2 at the highest point in the cupola before the effluent gas becomes mixed with air. Inasmuch as the gas composition does not change much at different levels in the middle third, the sampling points should be at least one-third of the way down to the tuyeres and above the melting zone. The position in the cross section is considered to be of more importance than the level as long as the places of sampling are restricted to the middle third.

As far as the balanced blast cupola is concerned, it should introduce nothing to interfere with the application of the equations as long as

the fundamental limitations are not violated. However, some of the qualitative conclusions may need to be altered. For example, air may be fed into the cupola at such a high level that there is little benefit in heating the iron, but a tremendous effect in changing the composition of the gas. The claim by British foundrymen that a CO content of 2 to 4 per cent can be obtained by using a balanced blast is accepted as being reasonable. The other claim of 0.5 per cent seems a little low. It would be interesting to know how low the CO can be held in a balanced blast cupola melting iron at 2800° F. The balanced blast cupola is not entirely unlike those with a double row of tuyeres, one row being about 12-in. above the other. Some, perhaps many, operators in this country have abandoned the use of both rows in favor of the lower. This evidence points to the fact that the practice of the balanced blast is not always as simple as it can be put on paper.

I appreciate very much indeed the fact that Mr. Phillips has given us the benefit of his experience in connection with the installation and operation of the hot blast cupola. His observations that the hot blast, where the iron-carbon ratio and air-volume were constant, gave faster melting and hotter iron are not questioned. However, there is ample reason to question the assumption that the hotter iron is accompanied with a higher CO_2 content in the effluent gas. From an examination of heat balances, it may be found that hotter iron may be obtained in spite of the CO_2 being lower. The equation of A-variables indicates that the melting rate cannot be higher with higher CO_2.

It may be generally recognized that a 72-in. cold blast cupola using an iron-carbon ratio of 7.0 and 7200 cu. ft. of air per min. may deliver iron at 2800° F. with an effluent gas containing 10.5 per cent CO_2. If a hot blast cupola operating under the same conditions would yield the same amount of CO_2, the difference in heat available for raising the temperature of the iron and other substances in the cupola would be exactly equal to the sensible heat of the blast. If room temperature is 70° F. and the blast is 800° F., this sensible heat would amount to about 100,000 B.t.u. per min. or per 500 lb. of iron. For each lb. of iron there would be 200 B.t.u. and, if the absorption efficiency is 60 per cent, there would be 120 B.t.u. used in actually raising the temperature of the iron. According to Umino, as quoted by J. W. Mellor in his book, *"A Comprehensive Treatise on Inorganic and Theoretical Chemistry,"* vol. VIII, p. 154, published by Longmans, Green and Co. (1934), the specific heat of pig iron is 0.215 in the molten state. The increase in temperature may be obtained by dividing 120 by 0.215 to get 558° F. Under such conditions the hot blast cupola would be expected to deliver iron at 3358° F. If the CO_2 content is assumed to be higher than in the cold blast, the calculated rise in temperature would be even more phenomenal. No one would claim that iron of this temperature has been obtained from the cupola, nor that the additional heat is in the slag or effluent gas, nor that it is radiated through the walls of the cupola.

If this additional heat does not manifest itself in the temperature of the products of the cupola, evidently it must be dissipated through some heat absorbing chemical reaction. The reduction of CO_2 with carbon to

produce CO is the primary heat absorbing reaction within the cupola. If similar calculations to those above are made with a certain assumed value of CO_2 lower than 10.5, the calculated rise in temperature may be 50° F. and is more in line with what might be expected. From these arguments and those made in the paper, it still seems reasonable that an increase in blast temperature will yield hotter iron with lower CO_2.

F. G. SEFING* *(written discussion)*: A controversy on "Combustion in a Cupola" seems to have developed more heat without than within the cupola. There are some statements in this paper that may be erroneously interpreted.

It seems that an important point lost sight of in this discussion is that cupola efficiency is not a matter of thermal efficiency, but one of melting efficiency. Therefore, our thinking must begin at temperatures where the iron melts, at about 2300° F., and at all times we must bear in mind the influence of time. A great deal of heat efficiently developed over a long time may never reach 2300° F., and, therefore, the melting efficiency would be zero while the combustion efficiency may be 100 per cent.

In paragraph 19, beginning line 8, there was one factor neglected here, namely, temperature. As the gases enter and rise through the bed their temperature is rising until they leave the bed, after which they cool continuously. Any change in temperature above about 1300° F. rapidly increases the CO to CO_2 ratio. Indeed, since the gas temperatures in the upper bed are at 3000° F. or above, the tendency is great for all the CO_2 to break down to CO in the presence of carbon. It is reasonable to say then, that in the vicinity of the melting zone we tend to have zero CO_2, even though the velocity of gases is high. Further, that as the effluent gases are cooling below 1800° F. above the melting zone, the CO is forming CO_2 according to reaction (4) in reverse ($2CO \rightarrow CO_2 + C$).

Regarding conclusion 14, it is possible to hold the blast temperature and iron-carbon ratio constant yet increase the rate of blast which increases the rate of combustion and the temperature and which will produce a higher CO. It is, therefore, incorrect to say that increase of CO indicates colder iron.

If the bases and terms upon which a discussion of cupola combustion rests are clearly stated, there will be no differences of opinion.

DR. PENNINGTON *(answer to Mr. Sefing's written discussion)*: Mr. Sefing has introduced in an impressive manner some important points for consideration.

It must be agreed that an extremely high stack, possibly out of the realm of the practical, would complicate the situation tremendously as far as the composition of the cupola gas is concerned, particularly if the sample is taken near the top. A higher stack would give more time for preheating and consequently a tendency toward a higher temperature with more CO. In comparing one process with another, it would be well to heed Mr. Sefing's implied warning that the same cupola, or two of the same height, should be used.

*International Nickel Co., New York.

In discussing my paragraph 19, the critic has called attention to a subject which has been hidden in a mantle of confusion ever since the first attempt was made to use the principles of chemical thermodynamics in cupola combustion. He makes a statement, which is quite true, that at the temperatures which exist near the top of the melting zone, the tendency is toward zero per cent CO_2. However, in spite of the tendency this condition, as far as magnitude is concerned, is hardly even approached. It still remains a fact that 10 per cent CO_2 may be found where the tendency is to have only 0.01 per cent. The amount actually found by analysis may be 1000 times the equilibrium value at temperatures higher than 2500° F.

One must not confuse a tendency with a magnitude of reaction. Hydrogen and oxygen tend to react at room temperature to the exhaustion of one or both of the gases but, in the absence of a catalyst, a mixture of the two gases behaves as though an equilibrium already exists. By use of the proper catalyst the reaction can be made to go practically to completion at room temperature. In such a case the thermodynamic tendency is not changed by the catalyst; it is the kinetic tendency that is tremendously increased.

Time is required to establish a chemical equilibrium. In some systems, the amount of time is indeed short; in others, many thousands of years may be required. There is some particular temperature for each reaction at which equilibrium can be attained in a comparatively short time. Hydrogen and oxygen may react to equilibrium within a fraction of a second at 2000° F., whereas a million years may be required at room temperature. The reaction of CO_2 with carbon is probably quite fast at temperatures near 3000° F., but rather slow at or below 2000° F. Being a heat absorbing reaction, the reactants and products are cooled as the reaction proceeds and, therefore, a temperature low enough for slow reaction is attained long before the CO_2 is exhausted. Unless this reaction is slow, below 2000° F., the theory of balanced blast must be modified. If the CO_2 formed from the burning of CO with air introduced through the upper tuyeres immediately reacts with carbon to form CO again, there can be no benefit from the use of balanced blast.

It is not generally realized how fast the gas in a cupola is moving. In order to get some idea of the speed, let us again consider the operation of a 72-in. cupola with an iron-carbon ratio of 7.0 and an air-volume of 7200 cu. ft. per min. At a point 7 ft. below the top of the burden, the temperature is probably close to 2000° F., while at the top of the burden it is near 1300° F. There will be an expansion over the original 7200 cu. ft. because of the temperature increase and also because of the formation of CO. Taking the average temperature as 1650° F. and the CO content as 18 per cent, it may be found that the volume of gas is about 33,690 cu. ft. per min. If the cupola interior consists of 40 per cent voids there will be about 79 cu. ft. in the 7-ft. section available for the passage of gas. The amount passing will fill the available volume approximately 426 times per min. or seven times per sec. This means that the gas is travelling at the rate of 49 feet per sec. or that 0.14 sec. is required for the gas to go 7 ft. Apparently this time is too short for

much reaction of a gas, containing 10 per cent CO_2 and 18 per cent CO, with coke if the temperature is less than 2000° F.

The question often arises as to whether the CO or CO_2 increases as the stack gases rise in the cupola. Evidently the change is not of sufficient magnitude that it can be easily determined by analysis. Massari, in his fourth discussion in connection with the paper by Massari and Lindsay[3], states that the CO_2 increases with increasing height. The author is inclined to the belief that instead of increasing, the CO_2 decreases as the gases rise in the cupola.

In order to set up a table of data to aid in arriving at a conclusion, it is necessary to obtain equations for calculating the equilibrium concentrations involved in the reaction,

$$C + CO_2 = 2CO. \tag{4}$$

By the law of mass action,

$$K = \frac{(A_{CO})^2}{A_C . A_{CO_2}},$$

where K is the equilibrium constant and A, with its appropriate subscript, denotes the activity of a certain chemical substance. If solid carbon is present, the activity of C may be taken as unity and the activities of the gases may be taken as equal to their partial pressures in atmospheres. Therefore,

$$K = \frac{(P_{CO})^2}{P_{CO_2}}. \tag{79}$$

Data published by Austin and Day in the January (1941) issue of INDUSTRIAL AND ENGINEERING CHEMISTRY have been used to get an equation in K and t. The logarithm of K plotted against the reciprocal of the absolute temperature results in a straight line, which may be represented by

$$\log K = 9.34091 - \frac{16363.6}{t + 459.4} \tag{80}$$

where t is ° F.

Equation (80) was used to calculate the equilibrium constants shown in column 2 of Table 9. Columns 3 and 4 give approximate values of CO_2 and CO known to exist in a cupola whose iron-carbon ratio is 7.0. It has been assumed that the CO_2 content decreases as the gases rise from some point where the temperature is 2000° F. to some other point where the temperature is 1300° F. The equilibrium compositions have been calculated by means of equation (79).

A sample calculation to show how the equilibrium concentration can be calculated is given for the case where the temperature is 1500° F. At this temperature, there are 9.85 per cent CO_2 and 18.25 per cent CO. Let X_r be the amount of CO_2 which reacts, then $2X_r$ will be the amount of CO formed. There will remain, after reaction to equilibrium, 9.85-X_r per cent CO_2 and the total amount of CO will be 18.25 + $2X_r$.

Table 9

CUPOLA GASES COMPARED WITH EQUILIBRIUM COMPOSITIONS

Temp. °F.		*Cupola Gas*		*Equilibrium Gas*	
	K	CO_2	CO	CO_2	CO
2000	465.0	10.00	18.00	0.03	34.50
1900	254.3	9.97	18.05	0.05	34.47
1800	125.4	9.94	18.10	0.09	34.39
1700	57.9	9.91	18.15	0.20	34.21
1600	24.8	9.88	18.20	0.46	33.79
1500	9.76	9.85	18.25	1.10	32.73
1400	3.47	9.82	18.30	2.63	30.19
1300	1.10	9.79	18.35	5.75	25.03

per cent. These last two percentages are on the basis of the original volume. They can be converted to the new volume basis by multiplying by the factor,

$$\frac{100}{100 + X_r}.$$

If the total pressure is one atmosphere, the partial pressure may be obtained by dividing the percentages by 100. It follows that

$$P_{CO_2} = \frac{(9.85 - X_r)\left(\frac{100}{100 + X_r}\right)}{100}$$

and

$$P_{CO} = \frac{(18.25 + 2X_r)\left(\frac{100}{100 + X_r}\right)}{100},$$

By substituting these values with that of K in (79), X_r and hence P_{CO_2} and P_{CO} can be determined. The percentages of CO_2 and CO can be determined in the equilibrium mixture by multiplying P_{CO_2} and P_{CO} by 100.

It is immediately evident that there is no value in column 3 as low as the highest value in column 5. Therefore, the reaction is, for all temperatures, on the CO_2 side of equilibrium and there is a tendency to form more and more CO as the gases rise in a cupola operating under the specified conditions. If the CO_2 had remained constant or increased in column 3, there would have been even a greater difference. The temperature at which coke is in equilibrium with a gas containing 10 per cent CO_2 and 18 per cent CO, where the total pressure is one atmosphere, may be found by equation (80) to be 1246° F. This temperature exists only outside of the cupola and inasmuch as CO cannot decompose until the temperature is lower than 1246° F., there can be no reversal of reaction (4) inside of the cupola.

If an iron-carbon ratio of 11.0 is used, the CO_2 and CO will be approximately 14.0 and 11.3 per cent, respectively. In this case, the temperature at which a gas of this composition is in equilibrium is

found to be 1120° F. which exists inside the cupola for the top gases may be as low as 900° F. In the very top section of a cupola, operated as specified, it is thermodynamically possible for some of the CO to decompose. In this case, if the gas sample is taken at a place where the temperature is higher than 1120° F., there can be no error due to the decomposition of CO.

In regard to the effect of changing the air volume, the critic has added to the confusion which has already been discussed in my reply to Mr. Vial. In referring to my conclusion 14, Mr. Sefing states that an increase in blast, with a constant iron-carbon ratio, will produce a higher temperature with more CO. It is a matter of record that the temperature will be increased, but as to the CO the truth is not all clear. As the situation now stands, Mr. Sefing thinks that an increase in blast will produce more CO; Messrs. Jungbluth, Korschan, Meyer, and Piwowarsky contend that an increase in air-volume has no effect on the composition of the effluent gas; and Mr. Vial and, surprisingly enough, the author maintain that there is less CO with increasing blast. There are now no more sides to the question and, therefore, confusion is at a maximum.

The statement that an increase in CO indicates colder iron, where the iron-carbon ratio and temperature of blast are constant, is based entirely upon the principles of heat balance. The heat developed, where more CO is present in the effluent gas, would be less per lb. of iron because the iron-carbon ratio is constant. If the amount of heat developed per lb. of iron is less, and the relative amount absorbed (absorption efficiency) remains the same or becomes less, it is axiomatic that the iron is colder. With an increase in CO the melting rate will increase giving the metal less time for the absorption of heat and, therefore, the absorption efficiency will decrease.

If the critic is correct in stating that increased blast gives more CO, an increase in CO will not necessarily indicate colder iron. Otherwise, higher CO is to be regarded as an indication that the iron is colder, the iron-carbon ratio being constant.

E. R. STARKWEATHER*: Dr. Pennington mentioned early in the paper the results obtained by running the cupola with just coke. I wonder why there should be the radical difference in these earlier results.

DR. PENNINGTON: This is a very pertinent question and one which we did not explain. Unless the oxygen of the air reacts with something else in the cupola besides carbon of the coke, the passing of iron through the cupola can exert no influence other than to affect the temperature and other factors dependent upon the temperature. There is reason to believe that the oxygen of the air does not react appreciably with anything except carbon, even though iron is being melted. In this case, all of the oxygen, with the exception of some 0.15 per cent, goes to form CO_2, whether the iron is present or not. However, if the oxygen reacts with something else, such as silicon, then the appropriate mathematical equation will be different from that which has been presented. If there

*Lufkin Foundry and Machine Co., Lufkin, Texas.

is considerable oxidation in the cupola, it may be due to the presence of much rust or other iron oxide in the metal charge.

MEMBER: Equations are always interesting, but when we try to apply equations in connection with a cupola, I believe that we have set out to do a pretty good job. Stability does not exist in a cupola, and to have an equation we must have stability.

We have, for instance, the formation of CO and CO_2 from coke. When CO_2 is generated, 14,500 B.t.u. per lb. of carbon consumed are available. When CO is formed, we have something like 10,000 B.t.u. per lb. less heat. I believe that every cupola operator is concerned with the equations representing conditions that exist inside a cupola. However, boiling it down to practical conditions, what we have is "Can we make good iron at low cost?" We cannot disregard the rate of formation of either one of these compounds inside a cupola.

Take a cupola and run it in many different ways, to form either a high CO content or a low content of CO_2 in the effluent gas. The point is, which condition gives us the iron that we must have? We encounter coke rates, for instance, anywhere from five to one to ten to one. Each operator makes a good iron for the practice against which that iron is built to serve.

When we try to reduce the cupola to an equation, we must consider what the over-all conditions are inside the cupola. Certainly we do not have conditions static to a point where they remain the same through any level or through any time.

In the first place, when we start to melt in the cupola, usually the bed is too high. At the end of the heat, in general, the bed is too low. In between, it is fluctuating by the depth of the coke charge. Consequently, to write an equation and say that we have this condition to form so much CO_2 or so much CO, the condition does not exist nor can it exist because the cupola is operating under fluctuating conditions.

That is an over-all picture. When we consider the cupola, and if we have the conventional six tuyeres, we have a condition in which we are varying throughout the whole area on this question of concentration of gases. The result is that in some places we have a high oxygen concentration and in others we have not. Take a cupola with other dimensions, we have this variable and we must decrease the concentration all the way up to the bed. The bed is put to a certain height for one reason only, to avoid deoxidation of the iron. Unless it is of proper height, the iron is burned. Oxygen is consumed by the carbon in the coke. Cokes vary, not only with respect to characteristics or burning qualities, but also as to size. They vary not only as to size at the start but the size that we end up with in the bed. Consequently, a formula or an equation is a dangerous thing to put out to the industry in general and say that the cupola can be worked from such and such an equation because, unless the exact conditions which have been observed in the formation of the putting together of that equation are observed, it will not fit.

DR. PENNINGTON: In the development of the various equations, the

fluctuations within the cupola were taken into consideration. It was not just a matter of taking a pencil and sitting down for the evolution of mathematical equations, but rather a matter of developing relations which could be applied in the plant operation of a 72-in. cupola. In order to allow for the fluctuations known to be present, a type of "operational equilibrium" was assumed. This is a sort of average condition and, therefore, the equation involving melting rate will not give an accurate result at any specific instant, but will give a value corresponding to a considerable period of time which is satisfactorily precise.

JOHN GRENNAN*: In regard to these equations, I have used a similar equation for a long time. I can write it in about three lines after boiling it down. This equation would avoid this antagonistic attitude among a great many of us who have read this paper.

It is generally accepted that the volume of oxygen (O_2) in the air is 21 per cent of the air. This oxygen will burn to 21 per cent CO_2, 21 per cent O_2 will burn to 42 volumes of CO. Adding 79 per cent for the rest of the air, we get 121 volumes from the 100 volumes of air. Dividing the 42 volumes of CO by 121 gives 34.7 per cent CO in the gases. The relation of CO_2 to CO is a straight line relation between 21 and 34.7 per cent, or approximately 3 to 5. To compute the relation of CO_2 to CO when the CO_2 is known, subtract the amount of CO_2 from 21 and multiply the difference by 5/3, which gives the amount of CO. For example, if the analysis shows 13 per cent CO_2, there would be 5/3 times 8 = 13.3 per cent CO present.

DR. PENNINGTON: Several people have used the same type of equation to get CO in terms of CO_2. Mr. Grennan's development is just as simple except it is a little more difficult to understand that the relation is a straight line. There is nothing new about the equation as it has been presented. The first equation (9) was developed as it was to illustrate or to justify a similar development for the equation (16) involving CO_2 which results from the decomposition of limestone.

The reason for developing equation (16) was not so much to be able to calculate the CO, where the CO_2 is known, as to keep the symbol for percentage of CO from occurring in the equation of A-variables.

One may go a step further and eliminate the percentage of CO_2 if a certain size coke, with a specific reactivity, and a definite type of scrap are always used. This requirement would be quite hard to meet and, therefore, it was not deemed advisable to attempt to reduce the equation to one involving only three variables. This equation of A-variables has not been offered as something compulsory for good operation of a cupola. Probably some use can be made of the equation if one cares to try. As an example, it has been said that 30,000 cu. ft. of air are needed to melt a ton of iron. A substitution of this value, along with other appropriate substitutions in the equation of A-variables, will result in a value of D, indicating whether it is true that 30,000 cu. ft. at 60° F. and 14.7 lb. pressure are needed. If R is 7.0, X will be about 11.5 per cent. On sub-

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stituting these values and 30,000 for C it will be found that the melting rate (D) is close to 60 tons per hr. or one ton per min., in which time 30,000 cu. ft. of air were used. On the other hand, if R is 14.0, X will be about 16.5 and D will be close to 100 tons per hr., or 5/3 instead of one ton per min., in which time 30,000 cu. ft. of air were used. In the latter case, then, only 18,000 cu. ft. would be required to melt one ton of iron. Thus the equation serves as a means of conveniently and quickly checking the volume of air required per ton of iron melting under specified conditions.

There are many reasons to justify the development of the equation of A-variables. It is impossible for us to tell you all of them. As another practical use, suppose it is desired to determine how much air is going through the cupola during a heat if the melting rate, iron-carbon ratio and CO_2 content are known. The equation can be used to determine how much air goes through, not how much is supposed to go through. Most blast volume-meters measure the air which passes a point in a pipe but, due to leaks, this amount of air per min. does not go through the cupola. An allowance of 15 per cent is sometimes made for leaks. How nearly correct is this figure and, for that matter, how nearly correct is the blast volume-meter? Because the equation of A-variables is based upon a material balance, it is believed that it may serve as an adequate tool for measuring air-volume.

W. J. BUVINGER*: I would like to ask one of the gentlemen who spoke if he considers that burning coke to 21 per cent CO_2 gives the best results?

MR. GRENNAN: In all the text books we will find the statement that burning coke to CO_2 gives the greatest B.t.u. That is a very simple statement of physics. But so far as operating a cupola and melting iron and getting all CO_2, it is impossible. We cannot melt the iron low enough and absorb the heat fast enough to keep that CO_2 from going to CO. I was simply saying that the first reaction was to burn carbon to CO_2, and the secondary reaction is CO_2 burning to CO, with a resulting drop in B.t.u. value.

Massari[3] definitely showed that a high CO_2 content in the cupola would burn up more silicon and manganese, and the value of the silicon and manganese would be greater than that of the additional coke. So, economically, high fuel efficiency is poor cupola efficiency. If we are going to melt iron with expensive silicon and manganese (and get a poor grade of iron) rather than by the cheaper coke method of melting, we can do it. We have had conditions where we melted the same iron and got twice as much silicon burned out during the melting as we did with higher coke ratios. But these are all problems with which the practical foundryman is faced. He is not interested to any great extent on how much B.t.u. value he gets out of the coke. He is interested in how much quality iron he gets out of his cupola.

They are not exactly the same problems. But a great many text books have been written and they say that CO_2 will go to CO in the cupola, and

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this reaction must be prevented. Massari's[3] results showed that it should be encouraged within limits, and that breaks down a lot of our theoretical computations that give us 15,000 B.t.u. under one condition and 4350 B.t.u. under another. It does not give us good iron. So that when we operate a cupola on a piece of paper or when we go out in a foundry and operate a cupola to produce iron, these are two distinctly different problems.

Most of my problems have been in the operation of a cupola to melt iron and figuring out some equations occasionally to show where the B.t.u. go. But one of the most satisfactory things that I have had on the technical side was that Mr. Massari came out and said, "Here are the actual facts. CO is a good, useful material in your cupola." Then we can become reconciled to the fact that we are burning up or destroying perfectly good B.t.u., according to the old theory, and melting iron with a high coke efficiency but a poor iron result.

DR. PENNINGTON: I should like to add a remark in connection with the amount of CO and CO_2 in the cupola. If there are enough heat units developed to melt the iron, it is not necessary to consider the total to get a definite measure of the melting rate. With specific settings of instruments the cupola will adjust itself so that iron is fed into the melting zone at a definite rate which is coincident with the melting rate.

As a matter of illustration, let us consider the melting of a metal in two electrically heated furnaces. Suppose in one 500 B.t.u. can be developed per hour; in the other 5,000,000 B.t.u. can be developed, and in both cases the temperature is held just above the melting point of the metal. If a gram of the metal is fed into each furnace per hr. in small particles added continuously throughout the hr., it is possible to have an average melting rate of 1 gram per hr. in each furnace regardless of the fact that one has 10,000 times the capacity of the other. It is, therefore, evident that the rate of melting is not necessarily directly related to the total heat which can be or is developed.

After sufficient heat has been provided in the cupola, the iron will melt and the furnace will behave as a cupola. In the normal behavior of the cupola, the melting rate will be dependent upon the rate at which the iron enters the melting zone. The rate of entering depends only on how fast the carbon (coke) gets out of the way so that the iron may, by gravitational pull, enter the melting zone.

In regard to the proper amount of CO for the successful operation of the cupola, it may be said that this depends on the kind of iron desired. It is a matter of practical knowledge that a hot cupola gives forth much CO which burns vigorously at the charging door. A cupola operating so as to melt iron at 2800° F. with an iron-carbon ratio of 7 will have much more burning (reaction of CO and air) at the charging door than one melting iron with an iron-carbon ratio of 11 with an iron temperature of 2680° F. There is a direct relation between the amount of CO_2 or the amount of CO and the iron-carbon ratio. In general, the higher the iron-carbon ratio, the more CO_2 there will be accompanied by colder iron. The reason for the colder iron is that, in spite of there being more heat units

per lb. of carbon, the increase in iron-carbon ratio gives so much more iron that the heat units per lb. of iron becomes less.

There are changes which can be made which will result in higher CO_2 with hotter iron. In this case the iron carbon ratio may be considered as being constant. For example, there is the factor of coke size. If it is true that larger coke will give more CO_2 where the iron-carbon ratio is constant, it will also give hotter iron because not only are there more B.t.u. per lb. of carbon but also more per lb. of iron. The coke, of course, must not be so large that it does not use practically all of the oxygen. If 1, 2 or 3 per cent of the oxygen goes through and out of the cupola, the above statement may not hold.

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